Optimization of a progressive microforming process and in-process material behavior

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**OPTIMIZATION OF A PROGRESSIVE
MICROFORMING PROCESS AND IN-PROCESS
MATERIAL BEHAVIOR**

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List of Abbreviations

ANOVA	Analysis of variance
BET	Block elemental technique
CLP	Closed lubricant pockets
DCE	Double cup extrusion
DLC	Diamond like carbon
DMZ	Dead metal zone
EBSD	Electron backscattered diffraction
EP	End position
ETP	Electrical tough pitch
FE	Finite element
FESEM	Field emission scanning electron microscope
GND	Geometrically necessary dislocation
HAGB	High angle grain boundary
IP	Intermediate position
IPF	Inverse pole figure
LAGB	Low angle grain boundary
MEMS	Micro electro-mechanical systems
OLP	Open lubricant pockets
PMMA	Polymethylmethacrylate
PP	Probing position
PS	Pressing start
RA	Recovery annealing
RCA	Real contact area
RSM	Response surface method
SSD	Statistically stored dislocation
TDC	Top dead center of the process
VGRAIN	Voronoi grain

Nomenclature

C	Constant value
h	Pin height (mm)
J^*	Upper bound on energy consumption rate; applied power (J/sec)
J_e^*	Extrusion component of the rate of energy loss (J/sec)
J_r^*	Ring compression component of the rate of energy loss (J/sec)
L	Die land length (mm)
m	Friction factor
P	Pressure (N/mm ²)
R	Radial position (mm)
R_0	Punch radius (mm)
R_i	Die orifice radius (mm)
R_n	Neutral plane radius (mm)
S_L	Area of velocity discontinuity (mm ²)
S_i	Surface over which tractions are prescribed (mm ²)
T	Blank thickness (mm)
T_i	Prescribed applied surface tractions (mm ²)
Δt	Time difference (sec)
V	Volume (mm ³)
Δv	Velocity difference (m/sec)
v_i	Velocity component (m/sec)
v_p	Punch velocity (m/sec)
$\dot{W}_f$	Frictional rate of power loss (J/sec)
$\dot{W}_i$	Internal rate of power of deformation (J/sec)
$\dot{W}_s$	Shear rate of power loss (J/sec)
x	Geometry size effect factor
y	Vertical position (mm)

$\dot{\epsilon}_{ij}$	Strain rate component (1/sec)
θ	Angular position
σ_0	Effective flow stress (N/mm ²)
τ	Shear stress (N/mm ²)
N_G	Number of grains over the diameter
N_{GS}	Number of subgrains over the diameter
u_x	Displacement in X direction
u_y	Displacement in Y direction

Summary

In this context, a progressive open-die micro-forging/extrusion process was introduced and developed for production of axi-symmetric micro-metallic components. Such a system has unique advantages over existing micro-forming processes. First, it circumvented the issues of handling small billets needed for extruding pins of very small diameter. Second, such a process of using a strip metal can be more productive, since a progressive process can be implemented. Third, the progressive manner provides the benefit of using a blanking process to remove the formed pin from the strip metal, eliminating the possibility of buckling damage if an ejector pin was used in the process.

Investigating the microstructural evolution during the process interestingly revealed the existence of a dead-metal-zone (DMZ) at the micro-pins' surface in some circumstances. Finite element (FE) simulation showed that due to the openness nature of the process, the material flows in two different directions, i.e. towards the die orifice and away, leads to develop a neutral plane in the deforming material. Knowing the location of the neutral plane using FE simulation, together with the shape of the DMZ obtained from the experiments, helped us in understanding the cause for this behavior. Having that found, selection of the optimum tooling dimensions (i.e. punch diameter) was suggested as a way to control the behavior of the DMZ in the pins' microstructure.

Using FE simulation, it was shown that the friction inside the die orifice plays a more significant role on material behavior, rather than friction on the die top surface. Statistical analysis using ANOVA method provided an analytical

solution for determination of the friction factor based on the relative punch and die diameters.

Experimental investigations revealed the prevention of the formation of the DMZ at the pin surface by increasing the initial grain size. This behavior was attributed to the role of the subgrain size on the deformation.

Moreover, micro-compression tests on the micro-pins having different grain sizes revealed no significant size effect with respect to the mechanical behavior, even if the number of grains over the diameter of the micro-pins falls below its critical value. To justify the reason laying under this fact, a recovery annealing cycle was applied on the micro-pins to change the substructure properties without altering the mean grain size. A surprising drop in the flow stress of the recovery-annealed micro-pins implied the importance of considering subgrain size rather than grain size over the diameter of component for the size effect investigation.

Besides, phenomenological studies showed that there are two main phenomena competing during the open-die forming process; (i) Upsetting, (ii) Extrusion. Since the main “pin forming” occurs during the Extrusion phenomenon (the last stage of the process), it can be used for process optimization. To do so, the Upper Bound Theory was successfully modified for finding the critical strip thickness, after which the Extrusion is the dominant phenomenon. This provided an analytical solution for optimization of the process by minimizing the material wastage.

Lastly, the effect of different lubricants on the material flow was investigated. It was shown that due to the process scale, it is difficult to put and keep the lubricant inside the die orifice, and hence no significant lubrication

effect on the material behavior could be observed experimentally. Theoretical studies using the developed Upper Bound model, however, showed that successful lubrication inside the die orifice could significantly affect the final part dimension.

Chapter 1: Introduction

1.1. Background and problem statement

Production of miniaturized metallic parts has been a demanding trend in many technical areas such as electronics and medical industries [1]. Among all the micro-manufacturing techniques, micro-forming is well-suited for efficient production of metallic micro-parts due to its unique advantages, including: (i) near-net shape production, (ii) improved mechanical properties of the final product, together with (iii) the mass production capability and (iv) lower manufacturing cost.

A few years after the introduction of the microforming concept by Geiger *et al.* [2], many researchers in this field were mainly focused in deformation mechanisms [3], material/tool interface conditions [4], material properties' characterization [5], process modeling and simulation [6], as well as process optimization [7].

Despite the extensive of research in this field, microforming processes have not been adopted extensively for mass production in the industry. This is so as there are issues such as handling of micro-parts and removal of the formed parts

from dies without damage, which require further process and manufacturing system design and development.

To resolve the handling issue, Sanchez-Salmeron *et al.* [8] reviewed several automated microhandling systems using different gripping principles such as friction, pneumatic, magnetic, electrostatic, and ultrasonic. The key challenges that are limiting the application of these microhandling technologies are low flexibility, high cost, and medium precision.

As an alternative, Engel *et al.* [9] reported that handling of parts is rather easier in sheet metal forming, since the parts usually remain connected to the strip. This is a big advantage for the sheet metal forming processes as they are miniaturized to the micron scale.

Following that, Hirota [10] suggested the use of a disk-shape specimen as a feed for the production of micro-billets. A counter punch was used under the formed pin to push it up after the pin forming process. However, as the pin diameter decreases in size and the surface area to volume ratio increases, such a pin removal method will become a challenge due to the greater risk for damage caused to the pin.

A split or segmented extrusion die could be an alternative for part removal as was used by Cao *et al.* [11] in their microextrusion experiments. However, using such a die, the process becomes intermittent making it not applicable for most manufacturing considerations. Furthermore, handling and transportation of small billets/pins becomes another concern, especially when the pin diameter gets smaller [12].

Based on the above mentioned technical gaps, a progressive pin forming die system was developed in this study, with the following advantages over other existing micro-forming technologies: (i) it can circumvent the handling issues of small billets needed for extruding pins of very small diameter. Taking an example, for the forward extrusion process having an extrusion ratio of 4, there is a need for the billet with a dimension of 1.2 mm in diameter and 0.5 mm in length to manufacture a micro-pin with a diameter of 0.3 mm and length of 2.4. Handling and positioning of such a small billet as a feed to the process can be a challenge; (ii) such a process uses a strip as the workpiece and is more productive since a progressive process can be implemented; and (iii) instead of ejecting, the system uses a blanking process to remove the formed pin from the strip material, eliminating the possibility of buckling damage if a counter punch is used as an ejector.

There is a lack of detailed studies on metallurgical aspects of micro-forming processes, and none exist in the area of progressive microforming. There is also very limited understanding on the microstructure's influence on the final micro-product properties. This is, however, very critical for process optimization [10, 11, 13-15].

Scaling down a conventional forming process to micro-scale, brings up the “size effects” influencing the material behavior [16]. Despite the huge number of the available reports regarding the interactive grain-specimen size effects on the material's mechanical response during microforming, there are limited

publications about the grain-specimen size effect on microstructural evolution during processing.

The material used in all the preceding “grain size-effect” reports was fully annealed before forming. It implies that the initial dislocation structure of all the previous studies was the same, with almost no developed dislocation cells or sub-grain in the microstructure before forming. Investigating this effect could be a great help in understanding the reason behind contradicting reports on size effects.

In addition to the experimental studies, an analytical modeling technique was developed to provide a universal solution for the process. Among all modeling techniques, the Upper Bound analysis has proven to be a powerful method to simply and quickly provide universal solutions within reasonable engineering approximations [17]. Thus, this theory was developed for the progressive micro-forming process. The effect of material flow inside the die orifice, and the relative die punch diameter effects were considered in our approximation.

One of the most challenging factors affecting the material behavior is friction; the frictional force not only determines the forming load, but also influences the pin quality. Furthermore, the accuracy of the modeling results in forming processes strongly depends on the frictional boundary conditions and even more so in the micro-scale [18].

A few friction models based on the surface quality of the die-workpiece were proposed that can be used in the simulation [19, 20]; however, measuring

the related parameters for these models in the micro-scale forming processes is rather difficult.

Recently, Chan *et al.* [21] reported that especially for the close-die forming processes in micro scale, the size effect must be considered in the mechanical behavior of material. Otherwise, it would be impossible to get a fit between simulation and experimental results [21]. Thus, it would be interesting to study the lubrication effects on the overall material flow using the Upper Bound model.

1.2. Objectives and scope

Based on the gaps in previous studies, the scopes of this research were defined as:

- i. To establish a comprehensive understanding of the material flow during the open-die microforming process, considering process parameters effects (e.g. punch and die diameter, friction, grain size).
- ii. To provide an analytical solution and to optimize the process into a sustainable microforming process with the least material wastage.

To this end, the plan for achieving the scopes includes:

- a) Study the microstructural evolution during the process.
- Fabrication of the micro-pins in various dimensions to show the feasibility of the process.
 - Study of the effect of tooling dimensions on the final part dimensions and the required forming load.
 - Investigation of the micro-pins' microstructure throughout the process, using optical and electron microscopy.
 - Identification of the dead-metal-zone (DMZ) in the microstructure and its behavior during the process. Microhardness test on the cross-section of the pins is used to prove the existence of the DMZ.
 - Using the FE simulation for deeper understanding of the material flow during the process.
 - Using ANOVA technique for prediction of the friction factor to be used in the open-die microforming processes for various dimensions.
- b) Analyzing the effect of initial material properties on the final part properties.
- Annealing the initial strips, with various thicknesses, at different temperatures between 400 to 800 °C using a vacuum furnace.

- Studying the mechanical properties of the annealed strips using compression test.
 - Using the EBSD technique to elaborate the reason behind the material behavior.
- c) Investigation of the post-production material behavior.
- Recovery annealing of the micro-pins having different grain sizes, at 230 °C using a vacuum furnace.
 - Designing a micro-compression test to get the mechanical properties of the micro-pins with different grain and sub-grain structure.
 - Defining the grain size effect using Hall-Petch equation.
 - Using EBSD technique to understand the effect of subgrain size on the overall post-production material behavior.
- d) Analytical modeling of the process
- Examination of the phenomena involving in the proposed process.
 - Developing the Upper Bound theorem based on the phenomenological study.
 - Definition and identification of the critical strip thickness for optimization of the process.

- Providing a universal solution for predicting the in-process material behavior.
- Elaborating the effect of lubricants on the process using the developed model. FE simulation is also used to examine the effect of the localized friction factors on the material behavior.

1.3. Organization of the thesis

The introduction chapter (Chapter 1, as you have gone through), provides a rationale for the research and establishes the goals and scope that are directed towards developing a deeper understanding of the metallurgical aspects of the open-die micro-forging/extrusion process.

In Chapter 2, a literature survey of the developed micro-forming processes is presented, with emphasis on size effects, friction challenges, and effect of process parameters on the material behavior.

In Chapter 3, the experimental procedures and characterization methods are described. After practical explanation of the fabrication stages, annealing methods and microstructural and mechanical characterization techniques are elaborated. Detailed information of the material to be investigated, tooling, and lubricants are provided. Lastly, the boundary conditions used in FE simulation are described.

The effect of process parameters on the microstructure of the micro-pins during the process is studied in Chapter 4. The unique material flow is elaborated using FE simulation, highlighting the important role of the neutral plane on the material behavior. Friction in FE simulation of the process is described in detail. ANOVA statistical analysis is used for prediction of the friction factor for different process circumstances.

Chapter 5 focuses on the interactive effects of grain size and initial specimen dimensions (pre-production properties) on the material behavior during the process. Tracking any possible size effects in the process is on interest of this chapter.

In Chapter 6, the effect of post-production annealing (final part properties) on the overall mechanical behavior is examined, with more focus on the substructural size effects. EBSD data analysis is used to justify the material behavior.

Chapter 7 focuses on the optimization of the process by developing an analytical Upper Bound theory. The model is modified using a geometry size factor, which considers the effect of relative punch and die diameter on the overall forming energy. Definition of the critical strip thickness, using phenomenological studies, helps in obtaining a universal solution for the process in dry condition.

In Chapter 8, the effect of lubricants on the material behavior is investigated using the developed Upper Bound model.

Finally, conclusions are given in Chapter 9, following by suggestions for future work in Chapter 10.

Chapter 2: Literature Review

2.1. Introduction

Over the last two decades, especially with the development of Micro Electro-Mechanical Systems (MEMS) and medical industries, the demand for smaller metallic parts has increased [7]. For mass production of these miniature parts, development of manufacturing processes in the micro-scale is essential. Micro-manufacturing, which is defined as a bridge between macro- and nano-manufacturing, is usually categorized into two groups of MEMS manufacturing and non-MEMS manufacturing. Processes such as photolithography and chemical etching are in the MEMS manufacturing processes category; while non-MEMS manufacturing category often involves techniques such as micro-machining, micro-embossing, micro-stamping, and micro-extrusion [22]. Among these processes, micro-metal-forming techniques for production of micro-metallic-parts have received a great attention during the last decade [23, 24]. Unique advantages such as near net shape and good mechanical properties of the final product, together with the mass production capability and lower manufacturing cost have made micro-metal-forming processes attractive [25].

Figure 2-1. Cold worked micro-parts [18, 26].

As a pioneer, Geiger *et al.* [18] defined microforming as the forming of metallic parts with at least two dimensions in the sub-millimeter range. There is a variety of microforming processes developed so far, such as micro-bending, micro-extrusion, micro deep drawing, and micro blanking. Connectors, contact springs, micro cups, micro shafts for micro-motors, micro pins, and lead-frames are some examples produced by these operations (Fig. 2-1) [2, 10, 11, 27-34].

However, metal forming in micro-scale, renders more challenges compared to the manufacture of macro-parts. Conventional rules that exist for the forming processes at macro-scale sometimes cannot be directly applied to the micro scale, and need adjustments. This is mainly due to the so-called “size effect”, which will be explained in detail in the sections ahead. The challenges in microforming are divided into four main groups in the literature: Material, Tools, Process, and Machines (Fig. 2-2) [18]. The most important categories of challenges are material and process, since the metal behavior mainly depends on these two categories. In this case, several studies have been done to understand the material behavior during microforming processes [7, 15, 16, 25, 35-44].

Since material and process are strongly related to each other, material behavior affects the process efficiency [18]. Thus, in our research, we focused on some of the challenges in the material and process categories.

For instant, process-material-related parameters such as forming force, friction and final part dimensions could be affected by specimen size and/or grain size, which subsequently affects the final mechanical properties [45, 46].

As for the challenges in the tooling system, manufacturing of tools in sub-millimeter dimensions with good mechanical properties and proper tolerance is rather difficult. Moreover, handling of micro-parts between process stages might need high-precision equipment [5, 8, 12]. Subsequent characterization of the produced parts has been also another issue in this case [6, 47, 48].

Figure 2-2. Challenges in microforming [18].

2.2. Categories of size effects

In all the challenging categories, the *size* is what matters. The term “size effect” (interactive effects of grain size and specimen dimensions) refers to changes in material’s properties due to miniaturization, which are related especially to the material and the process used [5, 49, 50].

The three main categories suggested by Vollertsen [51] for size effects are density, shape, and microstructure (Fig. 2-3). The categories' subgroups are presented in Table 2-1, as suggested by Vollertsen [51].

Figure 2-3. Three suggested categories of size effects. F is force, F_A is adhesion force, F_F is friction force, F_G is gravity [51].

2.2.1. Grain-boundary size effect

Grain-boundary size effect (used as "Density size effect" by Vollertsen [51]) occurs when the amount (size) of features inside the material (e.g. defects or grain-boundaries) remain constant by decreasing dimensions of the sample. As a result, the absolute amount of defects will be decreased. Under this circumstance, there is also a probability for some miniaturized samples to have defects, while others might

not contain any [16, 52]. When considering the grains as the feature, it refers to the grain-specimen interactive size effects [53-57].

Table 2-1. Different types of size effect [16].

Category	Subgroup	Models	Examples of size effects
Density	- Interface area density	Weibull model	- Scatter in flow stress
	- Line length density		- Yield strength of whiskers
	- Point defect density		- Changing failure strength of brittle material
Shape	- Shape balance	- Relation between forces	- Adhesion to grippers in microtechnology
	- Shape sum	- Relation between heating and cooling	- Stronger heating of large samples during forming
		- Relation between generation and effusion of gas	- Pore formation in large welds
		- Surface grain model	- Increase in flow stress with increasing size
Structure	- Characteristic length	- Strain gradient plasticity	- Hardness decrease with increasing indentation depth
	- Micro-geometry	- Dislocation drain strengthening	- Strength increase due to dislocation pile up at a surface layer
		- Dislocation starvation effect	- Tensile strength of whiskers
	- Secondary artifact	- Lubricant pocket model	- Increase of friction with decreasing size
		- Composite model	- Increase of normalized cutting force with decreasing cutting depth
		- Texture model	- Decrease in flow stress with increasing size
			- Decrease in flow stress with increasing size, if grain size is larger than the sheet thickness

2.2.2. Shape size effect

Obviously, the sample's volume is proportional to the relative cubic length (r^3) and its area is proportional to r^2 . Consequently, changing the relative radius leads to a change of surface to the volume ratio by $1/r$. In this case, the size effect occurs if the value of interest would depend on the sample's dimensions (i.e. friction, heating and cooling effect, surface grain effect that will be discussed in next section). Challenges in handling of micro-parts are a good example of this type of size effect. Indeed, because of change in adhesion forces (surface-related forces) and gravity force (volume-related force), it is difficult to handle tiny samples and release them from grippers [16, 51]. This challenge, which is still a hot issue in literature, was considered in our process design.

2.2.3. Microstructure size effect

The microstructure size effects are related to microstructural features, which are not only metallurgical microstructures like grain size or second phases, but also the geometry-related features such as surface roughness. In this case, the sizes of the microstructural features remain constant while the overall size of the sample changes. For instance, the size of dislocation loops in a material is intrinsic and cannot be changed. Therefore, dislocation loops cannot be generated in the material if the grain size goes below a critical value [58, 59]. Mainly for this reason, the Hall-Petch relation is not valid below a critical range of grain size (typically 10-20 nm) [60].

In particular, by scaling down the specimen size, a decline in flow stress is observed (grain-specimen size effect). This behavior was reported by Kals [33] in sheet metal forming of CuZn15 and CuNi18Zn20. In the case of bulk metal forming, this effect was investigated by upsetting test of Cu, CuZn15, and CuSn6 [2, 13, 61]. Similar results were obtained for tension of Al, Cu, Cu–13 at.% Al and CuZn alloys [33, 62, 63], bulging of CuZn alloys[44], and bending of Al and CuZn alloys [33, 63], confirming a significant variation in the flow stress with downsizing of the specimen. In contrast, there are some studies reporting opposite behavior; namely micro-bending of pure Ni [64] and micro-torsion of pure Cu [65], demonstrated raise in strength with decreasing the specimen size. This could be due to the in-homogeneity in the grain size distribution, and thus the average grain size was above the critical value for those samples.

Considering grains as the feature in the microstructure of a metal, if the size of grains kept constant, there will be a decline in the number of grains in the observed volume by miniaturization [66]. Owing to this fact, the individual grain orientation plays a more important role in the measured strength of samples. Hence, there will be a much higher scatter in results of mechanical tests [51, 67].

2.2.4. Grain size effect vs. Specimen size effect

In another approach, Armstrong [52] explained that there are two different phrases of “specimen size effect” and “grain size effect”, both have different effects on the strength of metals. The specimen size effect becomes significant when there is only a few grains through the specimen cross-section. In comparison, the grain

size effect comes into play when there are no sufficient grain boundaries present in the specimen to act as obstacles for dislocation movements [52].

In principle, size effect is all about the combination of the grain and specimen size effects. In other words, size effect on flow stress is described by T/D ratio, i.e. the thickness of the specimen over the average grain diameter. The T/D ratio explains the number of grains over the cross-section of the specimen. The size effect predominates when only a limited number of grains are presented through the cross-section [68, 69]. In fact, mainly based on the stacking fault energy of the metal, there is a critical number of grains across the thickness of the specimen, below which the mechanical behavior of metal will not follow the Hall-Petch relationship (Grain-Specimen Size Effect) [21, 57, 70-74] (8 for Copper and 5 for Nickel, to name a few [75]).

Identically, when the number of grains over the thickness decreases, there would be a range of grain sizes, in which the individual orientation of grains plays a more important role in deformation [76, 77]. In this condition, depending on the grain orientation, there will be a scatter in results of deformation tests, because: (i) the relative number of surface grains compared to the internal grains affect the initiation of the deformation, and (ii) uncontrolled orientation variation occurs in different specimens [78-81].

As an example, Fig. 2-4 shows the yield strength of aluminum 1100 obtained from the bending and tensile tests, respectively. As illustrated in this figure, yield strength decreases by a decline in T/D ratio as it approaches one, which is consistent with Hall-Petch equation. Whereas, for $T/D < 1$ the mean yield stress

increases by decreasing the T/D ratio, which is in contradiction with Hall-Petch equation. The reason for this phenomenon might be that in this range, the material behavior is dominated by a single grain orientation; therefore, if the grain orientation is not in a favorable orientation for mechanical test, the stress increases [52, 71, 81].

Despite the huge number of the available reports regarding the grain-specimen size effects on the material's mechanical response during the microforming processes, there are very limited publications about the grain-specimen size effect on material flow or microstructural behavior during these processes.

Figure 2-4. Yield strength of aluminum 1100 alloy (bending test) [31].

Recently, Chan and Fu [59] investigated the effect of grain size and specimen dimension on the mechanical behavior of metals. It was reported that at a given grain size, decreasing the specimen size reduces the grain boundary volume fraction, which in return affects the grain boundary strengthening effects. Moreover, smaller specimens could generate a lower dislocation density inside the grains to accomplish deformation. Therefore, both grain size and specimen dimensions contribute to the grain strengthening and overall material behavior during the forming process [82]. Based on this fact, a modified Hall-Petch equation was reported considering the interaction effects of specimen and grain size for fully annealed samples.

Keller *et al.* [75] focused on the microstructural investigation of the grain and specimen size effects as two separate parameters. It was shown that the reduction of the number of grains across the thickness below a critical value, leads to a decrease in flow stress, and a modification of the strain hardening behavior. They also evaluated the interactive grain and specimen size effects on the dimension of dislocation cells in different regions of the sample during sheet forming.

In another report [83], it was stated that there must be a delay in cross slip mechanism by increasing the number of grains across the thickness, which leads to a “softer” dislocation structure throughout the sample. Thus, the dislocation cell size (or subgrain size) becomes bigger in specimens with fewer numbers of grains over the thickness. It was also tried to define

a relationship between the dislocation cell size and the strain hardening during forming [84]. In a similar case [85], a change in the accommodation process during forming has been reported from the grain size level at low strains to the substructural level at large strains. In other words, the substructure plays the key role at large strains, as also mentioned by Thompson [86].

The material used in all the above mentioned reports was fully annealed before forming. It implies that the initial dislocation structure of all the previous studies was the same, with almost no initial dislocation cells or subgrain developed in the microstructure before forming. There has not been any specific report out in the literature investigating the effect of initial subgrain structure, or post-production properties' effects, on the mechanical behavior of miniaturized specimens.

As will be elaborated briefly in the next section, a few models were developed for explanation of the grain-specimen size effect.

2.2.4.1. Surface Grain Model

Based on restrictions beyond grain boundaries in a specimen, there are two types of grains: surface grains and internal grains. At least one facet of the surface grains is at the surface of the specimen. The internal grains are surrounded by other grains at all their boundaries. Having that mentioned, the strength of a specimen is a summation of the share of the internal and the surface grains. Therefore, keeping

the grain size constant, by changing the specimen dimensions, the share of surface grains increases (Fig. 2-5), which leads to a decline in flow stress [18, 87].

Figure 2-5. Share of surface grains by changing the sample or grain size, while keeping the shape constant [87].

In fact, the Hall-Petch relation accounts for the effect of stress concentrations at grain boundaries [88]. There is an additional effect to the hardening as mobile dislocation becomes immobile at the grain boundary. This contributes to the overall material hardening. However, this effect is soon overtaken by the subcell formation with forest of immobile dislocations hindering moving dislocations [88].

Furthermore, dislocations can be annihilated at free surfaces (surface grains), while there is dislocations' pile-up at grain boundaries [13, 89]. Therefore, the

higher the fraction of surface grains, the more dislocations annihilation occurs in the microstructure. This leads to decline in the strength of metal. Namely, the material behavior changes from polycrystalline to single-crystal material by increasing the surface grains fraction [87].

This model is also investigated numerically by Shen *et al.* [79] suggesting the whole flow stress of material to be considered as a weighted average of inner and outer grains' flow stress.

2.2.4.2. Strain Gradient Model

Conventional deformation models consider only homogeneous strains without accounting for any strain gradients. So, these models are unsuitable for description of any processes, which include non-uniform deformation, or high local strain gradients. Specifically, microforming processes contain large inhomogeneous deformations, which induce strain gradients in the specimen [68, 90-93].

Basically, in the strain gradient model, dislocations are divided into two different groups of geometrically necessary dislocations (GNDs) and statistically stored dislocations (SSDs). The SSDs are stored in material homogeneously and statistically distributed in micro scale; whereas GNDs are essential to accommodate in deforming material for strain gradients. For instance, in sheet bending the GNDs are stored in a layer beneath the surface, while there is not any dislocation in neutral layer at the center of sheet. The distribution of this type of dislocations depends on

the strain gradient [89, 94, 95]. It is worth noting that in the strain gradient hardening calculations, the effect of friction usually is ignored [95].

2.2.5. Forming at elevated temperature; a solution

One counter-measure to the size effect behavior is to do forming at the elevated temperatures. At elevated temperatures (i.e. around 100°C for brass alloy) deformation systems in grains will be activated more easily. Thus, the deformation behavior will be less affected by grain orientations; and the scatter in result decreases [96]. In this case, warm forming is defined as forming at temperatures above room temperature in which work hardening still occurs in the material and recrystallization may not take place (to avoid affecting the grain size and the work hardening properties) [97-99].

Figure 2-6 shows the micro-upsetting results for a stainless steel at various temperatures reported by Egerer *et al.* [97]. As can be seen in this figure, the flow stress and the variation in flow stress of the material was decreased by increasing deformation temperature. Consequently, forming at elevated temperatures decreases the scatter in results and makes the forming behavior more homogeneous; although, it might make the process more complicated.

This phenomenon has been also detected in the hardness test (Fig. 2-7) [97]. Although, the higher the working temperature, the higher the friction coefficient would be. This is not desirable and must be considered in process optimization [98, 99].

To name a practical example, Wulfsberg *et al.* [98] used sapphire tools for laser-assisted microforming. The die in their work was made of a light conductive sapphire tool which could transfer laser beam to the specimen during forming. Since the laser beam is concentrated, the heat could transfer very fast in the specimen, and the control of heat generation is easy [100]. However, this solution could not be considered in our study as the power needed for this purpose is so high. Furthermore, because of relatively high surface area of the specimen, the sample cools down quickly, and it is difficult to keep the temperature of the specimen constant during forming.

Figure 2-6. Flow stress and scatter in results of upsetting test at different temperatures [97].

The coefficient of variation represents the scatter in results of stress in tensile test.

Figure 2-7. Hardness profile of the samples at different strains and temperatures [97].

In general, elevated temperature microforming was not considered in our work due to its complicated nature in micro-scale, which makes the process less favorable for mass production in the industry. This option was only kept in mind, in case there is a significant scatter in our experimental results. However, the effect of warm temperature annealing (recovery annealing) was studied in a great detail, as will be seen in next chapters.

One of the most important parameters being affected by size effect is the friction coefficient during forming, which will be explained in detail within the next section.

2.3. Friction in microforming

2.3.1. Methods to determine friction coefficient

There is a large variety of tests for friction coefficient determination; although, only a few of them are applicable to scaling experiments [28, 76, 101, 102]. A conventional method to study the effect of miniaturization on friction (friction size

effect) is the ring compression test, for which an increase in friction is reported by scaling down [28, 41, 103]. In this test, changes in the inner diameter of a ring during upsetting test is used for determination of the friction coefficient [104]. This determination is done by fitting the experimental results with the one of the finite element (FE) simulations [103].

As an alternative, Tiesler *et al.* [105] reported the double cup extrusion (DCE) test for friction behavior determination in scaling experiments (Fig. 2-8). They showed that this test is more accurate, especially for backward extrusion or deep drawing processes, which involve more surface contacts. Indeed, DCE test produces a relatively high surface expansion and high pressures, which represents the actual situation for extrusion experiments [106, 107]. In this test, a solid cylindrical billet is positioned in a die between a stationary and a moving punch of identical shape. By moving the upper punch, material deforms and makes an upper and a lower cup. The fraction of the height of these two cups is used for determination of the friction factor. In the theoretical case of zero friction, the height ratio is equal to 1; whereas, by increasing the friction coefficient, forming of the lower cup is prevented more (Fig. 2-8) [107].

Figure 2-9 shows some tested specimens cut into half. The comparison between friction conditions by changing the specimen size can be directly determined by comparing the height ratios. However, to determine the actual friction for a certain cup height ratio, FE-simulation is required [18].

Figure 2-8. Schematic setup of DCE test [107].

Figure 2-9. DCE-test specimens with various sizes [18].

An example of the DCE-test result is shown in Fig. 2-10 for CuZn15 alloy. As clearly illustrated in this figure, friction increases by decreasing specimen diameter. Scatter in results also increases by scaling down the specimen dimension. This behavior can be explained by the lubrication pockets model, which will be explained in the next section [18, 108].

Important to note that, Mori *et al.* [109] found that there is no significant effect of the grain size on both the static and the dynamic friction coefficients.

Figure 2-10. Results of the DCE-test determined by FE-simulation [18].

2.3.2. Lubrication pockets model

Based on the lubrication pockets model, micro-asperities at the specimen's surface (roughness peaks) start to deform plastically as a forming load is applied to the specimen surface, while the valleys remain un-deformed (Fig. 2-11).

Figure 2-11. Change of surface features during forming [103].

The lubricant escapes out to the surface from those valleys which have a connection to the edge of the surface (Fig. 2-12-a). These are so-called open lubricant pockets (OLPs). In this case, the lubricant is not able to transfer loads on the surface. Therefore, the forming pressure only acts on asperities, which makes the contact surface more flatten. As a result, there will be a higher fraction of real contact area (RCA), and thus higher friction [110, 111].

In comparison, closed lubricant pockets (CLPs) do not have any connection to the edge of the surface. Consequently, the lubricant gets trapped in these valleys, and it can transfer load on the surface. The resulting hydrostatic pressure reduces the normal pressure on the asperities, which leads to a lower friction (Fig. 2-12-b) [107, 112].

Figure 2-12. Effect of: (a) OLPs, and (b) CLPs; on friction [107].

Due to the topology rules [113], there is always a constant distance from the edge of a specimen where the OLPs are more effective (denoted as s in Fig. 2-13). By miniaturization, the fraction of these OLPs increases, as shown in Fig. 2-13 [103, 107, 112].

Figure 2-13. Effect of specimen size on OLPs and CLPs fractions [107].

Indeed, the fraction of RCA, CLPs, and OLPs depends on the specimen dimensions [105]. Thus, it can be used for investigation of scale effect. Figure 2-14 shows an example of the fraction of these areas through the depth of a specimen. As can be seen in Fig. 2-14-b, there is a constant rim near the edge of the specimen where the OLPs are dominant. Therefore, by decreasing the specimen dimensions, the fraction of this rim increases [107, 114].

Having that stated, it might be argued that there would be no size effect, if the surface roughness is scaled down to the sample size. However, as reported by Bech *et al.* [115], this is not the case. If the surface features get down-sized to the order of the sample size, the surface area is reduced by r^2 , while the volume of the pockets is reduced by r^3 . Consequently, there will be relatively less lubricant on the surface of the micro-parts than that of macro-parts. This leads to a weaker lubrication effect, and an increase in the coefficient of friction for the micro-parts [115, 116]. The increase in friction stresses can result into galling, excessive pressure on tool, and reverse forming [114].

Figure 2-14. Surface analysis of a typical specimen; (a) exemplary presentation; (b) distribution of surface fractions (RCA, black; CLP, white, OLP, gray) [107].

In comparison, for dry condition, especially when a soft metal slides on a hard tool surface, a steady increase in the relative RCA with increasing the sliding length was reported [111, 117, 118]. This explains the cause of increase in friction coefficient by starting the forming processes in dry condition. Provided that, there have been a few models developed to explain the frictional behavior via investigating the surface roughness [19, 112]. However, as reported by Jeon [19], a few of these models are not accurate in the case of high friction stresses (high pressures).

2.3.3. Solid lubricants; a solution to friction size effect?

In general, utilizing liquid lubricants could cause the size effect. As a solution, solid lubricants sticking on the surface of the specimen can be used [107, 112, 119,

120]. For instance, high-strength wear resistant coatings such as titanium nitride (TiN), chromium nitride (CrN), diamond like carbon (DLC), and molybdenum disulphide (MoS₂) have been used for reduction of friction forces in the extrusion process [15]. However, as the die diameter gets smaller, it will be technically very difficult to coat inside the die orifice. As will be mentioned in next chapters, this was one of the main challenges we faced in our research.

As another alternative, ultrasonic vibration has been suggested to be used during microforming processes [121]. This is not only to reduce the forming force, but also to improve the surface quality of the final product. The vibration reverses the frictional force direction for every period of ultrasonic oscillation. Therefore, the tribological performance of the system can be improved [122, 123]. However, this technology still has some drawbacks due to its high cost, and localized increase in the interfacial temperature on the die surface, which decreases the die life time. The ultrasonic system also consumes a relatively high power of energy.

2.4. Other process parameters

Eichenhuller *et al.* [124] studied process parameters' interaction in microforming of a pure copper. As shown in Fig. 2-15, effects of five major parameters were considered on the backward micro-extrusion process (Table 2-2).

Figure 2-15. Relevant parameters for the investigation or parameters interaction in Eichenhuller *et al.* work [124].

Table 2-2. Parameters range in Eichenhuller *et al.* work [124].

Parameter		Lower limit	Upper limit
Temperature	T	20°C	200°C
Grain size	l_g	Fine grained (28 μm)	Coarse grained (80 μm)
Punch velocity	v	3 mm/min	30 mm/min
Lubrication	lub	none	Drawing oil
Pin diameter	d_p	0.5 mm	2.0 mm

It was reported that temperature had the largest influence followed by die diameter and lubrication. Interestingly, Eichenhuller *et al.* [124] reported that lubrication has almost no effect on the final pin length. Although this may be true, they only examined extrusion oil as liquid lubricant, and did not mention anything regarding solid lubricants.

Besides the parameters affecting the microforming processes, one needs to know how to get a well-designed process for mass-production of micro-parts. In the next section, some case studies will be explained to have an overview on the improvements in microforming processes, and also to discuss practically about the previously stated facts.

2.5. Micro-extrusion Processes; Case studies

A few years after the introduction of microforming processes by Geiger *et al.* [2], a Japanese group in National Institute of Advanced Industrial Science and Technology invented a universal micro-extrusion machine, called the “microfactory” machine [125]. The machine consists of a lathe, a milling machine, handling devices and a press. Although the microfactory machine was not suited yet for mass production, it was an appropriate step towards industrialization of the micro-manufacturing technologies [126]. Tanaka [127] also reported a portable microfactory machine for production of specified microparts with low power consumption.

2.5.1. Micro-extrusion using a billet as a feedstroke

Later, Cao *et al.* [11] performed microextrusion experiments on a CuZn30 alloy using a segmented extrusion die, in order to facilitate the removal of the micro-pin after extrusion as well as studying the die surface roughness effects (Fig. 2-16). Billets with a diameter of around 0.756 mm were used as the feed for the process.

The extruded pin diameter was 0.568 mm. Such a process is an intermittent process and is not applicable for most manufacturing considerations, as after the forming of each micro-pin, the die must be split for micro-pin collection.

The effect of the grain size on deformation during the process was investigated by microhardness tests and also a brief microstructure evolution. It is worth noting that the process was a closed-die forming process (i.e. the metal has only one direction to flow – towards the die orifice). Thus, it would be interesting to study the effect of grain size on an open-die micro-forming process, where there are various directions for the metal to flow during the process.

Figure 2-16. Microextrusion setup used by Parasiz *et al.*: (a) segmented die; (b) forming assembly; (c) loading stage; (d) force-stroke response [14].

Cao *et al.* [11] used two billets of CuZn30 with grain size of 32 and 211 μm . As illustrated in Fig. 2-17, the pin with coarse grains (about four grains over the diameter) has the tendency to curve [74]. In comparison, this phenomenon was not seen for the pin with finer grain size (32 μm). Indeed, in the coarse-grained sample, the orientation of individual grains in the microstructure plays a key role in the deformation behavior. Conversely, since there are lots of grains across the diameter of the pin in the fine-grained sample, the individual grains' orientation is not as important as for coarse-grained material. Therefore, there would be more inhomogeneous deformation in coarse-grained material [14].

Discussing about the grain size effect, Chunju *et al.* [128] reported that in the micro-coining process, when the size of grains is near to the size of the die orifice (i.e. just a few grains flowing towards the die orifice), it is difficult for the grains to flow inside the die orifice. For instance, it is very difficult to deform aluminum in the process when there is only 2-4 grains involving in the deformation [129, 130].

Figure 2-17. Extruded micropins with diameter of 0.57 mm with different grain sizes [14].

As a tool to understand the material behavior during the process, mechanical properties must be studied together with microstructure. Although the micro-compression test was suggested as a more adequate way of mechanical characterization [131], due to its complexity, Parasiz *et al.* [14] used microhardness test for that purpose, as did Chen *et al.* [132]. Figure 2-18 shows the convention they used to define the directions.

Microhardness profile of the initial billets in radial direction is shown in Fig. 2-19. There is a lower hardness value in the center area, especially for coarse-grained specimen. This could probably be due to higher dislocation densities at the surface of the billet produced during a previous drawing process [14].

In detail, the curve in Fig. 2-19 is not symmetric and the minimum hardness is off- centered. There was no explanation to be found for this phenomenon, but it can

be assumed that it was due to misalignment in the die set, which caused asymmetric hardness results. It could be also because of grain orientations. In other words, in the case of the coarse-grained specimens ($211\ \mu\text{m}$ grain size), the order of step sizes (changes) in the microhardness curve is in the order of grain size. This indicates that grain orientations might cause this asymmetry.

Figure 2-18. Axial and radial location definition for extruded pin used by Parasiz *et al.*

[14].

Figure 2-19. Microhardness values of un-deformed billets with different grain sizes [14].

The extruded pins were investigated in the same way. Figure 2-20 shows the microhardness results for the reduction section of extruded pin with grain size of 32 μm . By moving from bottom to tip of the pin, the hardness values increased. This is due to more work hardening during extrusion. Comparatively, the hardness value across the radial direction is almost uniform. The same trend with more scatters was obtained for the coarse-grained pin [14].

Parasiz *et al.* [74] stated that due to the shear strain gradient, the surface grains experienced shear deformation, while the central grains just elongate due to the compressive forces (Fig. 2-21). Therefore, the surface region of the pin structure experiences more strain hardening.

Figure 2-20. Microhardness values for the 32 μm grain size pin [14].

Figure 2-21. Schematic of deformation modes during extrusion; (a) pure elongation in the center since the shear deformation is less and, (b) the additional shear deformation at the outer edge [74].

The area of interest is on the extruded part of the pin. It was observed that the microhardness value at the edge of the pin is much higher than those for the center, all along the pin length. This is due to the fact that the material at the surface of the pin undergoes more shear deformations rather than the material inside (at the center) [14]. This is in consistent with the strain gradient hardening model reported by Parasiz *et al.* [95].

Moreover, it was found that the microhardness value at the center part of the coarse-grained pins is higher than that of the fine-grained pins. It was mentioned that this was due to the fewer grains across the diameter for the coarse-grained specimen [74]. Thus, there would be more geometrically and statistically dislocations generated during deformation. In addition, grains at the center of the coarse-grained pin are just four or five grains away from the edge of the pin. Therefore, the central grains in coarse-grained pins undergo more shear deformations as compared to those of fine grained pins (deformation size effect) [14].

Furthermore, since the tip part of the pin is the first part that goes through the die orifice, it undergoes less deformation. The rest of grains in the specimen must rotate, deform and flow inside the die orifice. Consequently, the tip part has the least hardness values across the pin axial direction.

Their results showed that the mean hardness values for the coarse-grained pin are slightly higher than for the fine-grained pin, which is in contradiction with Hall-Petch equation. Although the difference was very small, they explained this behavior by the strain distribution model [14].

It is worth mentioning that in their investigations, Parasiz *et al.* [14] did not examine the effect of grain size on work hardening effect of material.

The microstructures of extruded pins are illustrated in Fig. 2-22. The microstructure was similar to the typical microstructure of extruded metals: it was mainly consisted of deformed grains with more deformed material at the edge of the pins. For the coarse-grained pin (Fig. 2-22-b), the grains just rotated and elongated along the tensile direction [14]. They did not report any details about the effect of process parameters on the microstructure of the deformed pin.

In the above-mentioned process, the grain size effect has been studied in a single directional flow manner, where the metal is constrained by die walls. Thus, an interesting question comes into mind: how about the grain size effect in open-die forming processes, where the metal can flow in different directions? Do we still see the same microstructure and microhardness profile throughout the pins?

Besides, handling and transportation of such small billets/pins becomes another concern, especially when the pin diameter gets smaller [12]. Sanchez-Salmeron *et al.* [8] reviewed several automated micro-handling systems using different gripping principles such as friction, pneumatic, magnetic, electrostatic and ultrasonic. Razali *et al.* [133] also reported the development of two new feeding (handling) systems using linear motor feeder and a micro servo roll feeder for a series of microforming processes, which still needs improvement for industrial applications. The key challenges that are limiting the application of these micro-handling technologies are the low flexibility, high cost, and medium precision.

Figure 2-22. Microstructure of extruded pins with grain size of: (a) 32 μm , and (b) 211 μm [14].

Using sticking forces to hold and transfer the micro-specimens, Arentoft *et al.* [134] developed an integrated two-step system for microforming (Fig. 2-23). The process has the capability for production of micro-parts with a reasonable production rate, using billets as the feed for the process. However, a precise cropping process was needed for the micro-billet preparation as the feed for the microforming process, which could increase the process time and cost. In this process, the pneumatic ejectors were used for micro-pin ejection from the die.

Figure 2-23. Schematic view of the developed feeding and transfer system. The billets are feed in a position 1 and the two forming cells are located at position 2 and 3. The finished component is ejected at position 4 [134].

2.5.2. Micro-extrusion using a sheet metal as a feed-stroke

To think about a solution for the handling challenge, Engel *et al.* [135] stated that handling was less difficult in sheet metal forming processes, since the parts usually remain attached to the strip. So the whole strip needs to be transferred/positioned instead of the individual miniaturized parts. This is a big advantage for the sheet metal forming processes as they are miniaturized to the micron scale.

Figure 2-24. Experimental condition of microforming process used by Hirota [10].

Using that concept, Hirota [10] suggested taking a disc specimen as the feed for billet forming process. A blank holder on top of the specimen controlled the constraining conditions (Fig. 2-24). A punch with a diameter of 2 mm was penetrated into the specimen and the billet with a diameter of 1 mm would be formed inside the die orifice under the punch [10]. To eject the billet out of the die

orifice, a counter punch was used under the formed pin. The issue with the counter punch, however, is that as the pin diameter decreases, such a pin removal method will become a challenge, due to the greater risk for damage caused to the pin.

Hirota [10] used finite element (FE) simulation to show the effect of constraint conditions on the metal flow during extrusion for manufacturing micro-billets. By this way, he could compare the results for the open-die and close-die forming processes. It was found that the billet height is mainly affected by horizontal constraints (Fig. 2-25). In the case of no constraint, the material mostly escapes outward and there would be a swelling around the billet in the final product and the billet height would be shorter, as shown in Fig. 2-26 [10]. By increasing the horizontal constraint, the amount of swelling around the pin will be decreased, and as a result; the pin height would be increased. However, this way the process will be intermittent and will be problematic in industrial applications.

Figure 2-25. Billet height and dimensional changes with various constraint conditions for: (a) smaller (2 mm Ø); and (b) bigger (5 mm Ø) initial diameter of sheet [10].

As illustrated in Fig. 2-25 using bigger disk as the feed for the process, the dimensional change is higher than that of smaller disk. This is due to lower rigidity (constrain) of the bigger specimen. It was also concluded that the condition (iv) which was completely constrained, would provide the longest billet [10].

Constraint conditions can also affect the punch force and displacement. It was stated that in conditions (iii) and (iv), in which there is a lateral constraint, the punch force gradually increases with increasing stroke. This is due to the resistance of material to flow towards the die orifice [10]. As shown in Fig. 2-26, the constraint condition affects the direction of the material flow as well. In condition (iv) where the specimen was completely constrained (laterally and vertically), no swelling was observed, and the billet length was relatively higher. In this case, the material can only flow towards the die orifice.

Figure 2-26. Swelling effect in micro-billets [10].

More experimental observations showed that in some cases, there were piping effect on the head part of the specimen after pin forming [10]. To find out the reason, Hirota [10] studied the strain pattern of the material during the process using DEFORM-2D software (Fig. 2-27).

As shown in Fig. 2-27-a, there was a shear deformation zone between the punch and die edges, and the material inside this shear zone was extruded to the die. Due to friction at the die surface, there is a large strain at the surface of the specimen. Therefore, the material at the core of the specimen flows faster into the die orifice. This leads to an inhomogeneous deformation flow, which in return results in the inhomogeneous mechanical properties. The inhomogeneous deformation flow behavior increases by increasing the punch stroke and results in piping effect at the end of the billet (Fig. 2-27-b). Hirota [10] found that if the punch penetration is less than 75% of sheet thickness, the piping effect could be prevented. However, he did not mention anything about the effect of the punch/die diameter on the piping effect. Therefore, as another target for our studies, it was interesting to investigate the effect of punch and die diameters on the overall metal flow during the process.

Figure 2-27. Material flow pattern during billet formation under the condition of complete lateral constrain for: (a) Punch stroke of 35% thickness; (b) Punch stroke of 90% thickness [10].

It is important to note that with regards to FE simulation, there existed differences between experimental and simulation results. This was attributed to the fact that the elastic deformation of material was not considered in simulation. It could be also due to the fact that Hirota [10] did not consider the critical effect of friction on deformation during the process. Indeed, by decreasing the billet diameter, the friction factor inside the die orifice was expected to play a more important rule, which has not been covered in literature. This concept was used in our studies to approach the handling challenge.

Of course, design and optimization of such processes require mathematical modeling before experimental tests. However, the discussed challenges make it difficult to predict the real material behavior during the process via simulation and modeling [136].

2.6. Analytical modeling of microforming processes

To obtain an accurate solution in a metal forming process requires a deep knowledge of the influence of different parameters such as material properties, tribological conditions, tool geometry and velocity. The characterization of the interaction between these parameters is also necessary for process optimizations, which can be accomplished easier through modeling techniques [137]. Indeed, the objective of the modeling is mainly to save time and reduce the costs by eliminating or minimizing trial-and-error tests for process design [138-140].

A variety of different modeling methods have been used to find solutions for real problems in metal forming processes, including finite element (FE) modeling, slip-line field theory, and upper/lower bound analysis (limit analysis).

Although FE modeling provides a rather accurate and visual description of material flow, stress and strain distribution during the process, it requires a relatively considerable amount of computing time; in some complicated cases a few days to get an accurate result for just one specific case [141]. More importantly, FE modeling is unable to provide a so-called comprehensive solution, and it needs to be conducted for each specific condition on a case by case basis. For the slip-line theory, it is more complicated and has mostly been successful only in analyzing plane-strain conditions.

In comparison, the limit analysis has proven to be a powerful method to simply and quickly provide universal solutions within reasonable engineering approximations [17]. Two solutions have been developed in this method: Upper bound, and Lower bound. Generally, the exact required forming power is located between the power predicted by upper bound and lower bound analysis. However, the stress-based lower bound solution is usually more difficult to develop. Furthermore, for an upper bound solution, the actual required forming load is never higher than the predicted load. Thus, it is more practical for metal forming analysis [142].

The main aim of doing analytical modeling in our work was to develop a model to provide a universal solution for design optimization of the axisymmetric open-die

forging/extrusion processes. Through this, it was possible to theoretically predict the forming load, critical blank thickness and the final part dimensions.

2.6.1. Upper Bound theorem; Introduction

The upper bound technique not only predicts the required forming load, but also has the capability of process optimization, and can be used for understanding the material behavior during the forming processes [143, 144]. Avitzur [17] used upper bound analysis for calculation of the forming power and prediction of flow pattern in a ring compression test [145]. The effect of friction and geometrical conditions on the material flow [145] and forming pressure [142] was investigated in the ring compression test [146], developing the concept of the neutral plane or “no-slip circle” which will be introduced and discussed in future sections [147]. This technique was also applied for the heading and extrusion process by Kudo [147] and Avitzur [17]. It is to be noted though that Kudo [147] did not consider the effect of material flow inside the die orifice on the overall energy loss during the so-called “heading” process. Avitzur [17] also did not consider the effect of material flow inside the die orifice for the conventional extrusion process.

In another application of the upper bound theory, Ajiboye and Adeyemi [148] investigated the effect of die land length on the forming load of the extrusion process using the upper bound method. The optimum die land length was suggested for different extrusion die opening shapes [149].

Later, Agrawal *et al.* [150] used the upper bound approach for optimization of the blank thickness in a deep drawing process. This is an important aspect of the deep drawing process, since it could minimize the earring defect and also decrease the required drawing force. The limit analysis technique was also used similarly to optimize the stamping process [151].

2.6.1.1. Phenomenological studies for the modeling

To optimize a forming process using modeling techniques, a phenomenological investigation of the material behavior during the process is needed [152, 153].

Discussing about the open-die forging/extrusion or “heading” process, Vickery and Monaghan [154] reported that the process has three stages. In Stage I, the process is similar to the compression of a cylinder with little or no extrusion through the die orifice. During this stage, the material only flows outwards from the central axis. In Stage II, the extrusion begins, but it is not a dominant phenomenon yet. During this stage, the total height of the pin remains almost constant while the flange thickness continues to reduce. Thus, there is a balance between forging and the extrusion phenomenon in the process. In Stage III, extrusion is the dominant flow process, and material prefers energetically to flow inside the die orifice rather than outwards.

As a conclusion, there must be a critical blank thickness after which the extrusion is the dominant phenomenon, in this type of metal forming process. This can be used to successfully optimize the axisymmetric sheet metal forming

processes, to reduce the material wastage and also the required forming load. This has not been reported to date, to the authors' knowledge.

The initiation of each stage in the process is defined by the location of the neutral plane. Indeed, the open-die forming processes can be controlled precisely by investigating the location of the neutral plane during the process. As initially discussed by Avitzur and Sauerwine [155], the location of the neutral plane is a function of the blank geometry and friction conditions [152]. It is worth noting that by changing the blank thickness, the location of the neutral plane changes to minimize the overall required energy for forming process [156].

2.6.2. Upper Bound theorem; basic formulation

A review and description in detail of the upper bound theory can be found in [17]. The following provides the basic model used in the upper bound theorem: "Among all kinematically admissible strain rate fields, the actual one minimizes the below expression"

$$J^* = \dot{W}_i + \dot{W}_f + \dot{W}_s$$

$$J^* = \frac{2}{\sqrt{3}} \sigma_0 \int_V \sqrt{\frac{1}{2} \dot{\epsilon}_{ij} \dot{\epsilon}_{ij}} dV + \int_{S_L} \tau |\Delta v| ds - \int_{S_f} T_l v_i ds$$

(2-1)

Basically, the actual required forming power J^* is less than that computed by using Eq. (2-1). The first term ($\dot{W}_i$) represents the internal power of deformation over the volume of deforming material. The second term ($\dot{W}_f$) expresses the shear

power over surfaces of velocity discontinuities including the tool-material interfaces. Friction stress is prescribed as constant shear stress,

$$\tau = \frac{m\sigma_0}{\sqrt{3}} \quad (2-2)$$

The friction factor m is considered constant for given process conditions under constant surface and temperature conditions. The maximum shear a material can stand according to von-Mises yield criterion is $\frac{\sigma_0}{\sqrt{3}}$, meaning that the limits of m are $0 \leq m \leq 1$.

The last term in Eq. (2-1), $\dot{W}_s$, includes the power supplied by body tractions, such as back stress applied in the wire drawing or back and front tensions in rolling processes [17].

To simplify the calculation procedure in upper bound approach, the Block Elemental Technique (BET) was introduced by Kudo [147] and developed by Avitzur [17]. This technique involves the division of the deforming material into basic elements. The velocity of each surface of each element is allowed to vary. The velocity of each element is constant inside the element and is constrained by the geometry of the process, tooling and by the velocities of the neighboring elements [157]. The flow pattern that requires the least amount of forming load is the one that actually occurs [145].

The following assumptions are made for applying the upper bound method [17]:

- The deforming material is isotropic and incompressible, and the elastic deformation is neglected.
- The shear friction stress is considered constant at the die-blank interface during the process.
- The material follows von-Mises flow rule.
- The effect of strain hardening as well as the temperature effect is neglected.
- The material flow outside the punching area is neglected, as by using the critical thickness, there is very limited flow outwards.

It is worth noting that the neglected parameters were considered to ease the complexity of the analytical method. The effect of these parameters, however, can be evaluated using FEM.

2.7. Summary

The main challenge in production of micro-metallic-parts, or so-called size-effect, was described including its different categories. The frictional behavior of workpiece-tooling in the micro-scale and the challenges related to this parameter was reported. The historical progresses on micro-pin forming processes, including micro-extrusion were reviewed, indicating the gaps in previous studies. Finally, the upper bound theory was introduced as a promising method for process optimization and for understanding the material behavior during the process.

In review of the past efforts, the aim of the current work was established as understanding of the in-process material behavior and optimization of a novel progressive microforming process. This novel process, as it will be explained in the next chapter, is a type of open-die forging/extrusion process using a sheet metal as a feed. From this study, the effects of friction and the grain size and specimen dimensions of the material flow are investigated. The experimental findings helped us to understand the mechanisms involving in the process and subsequently elucidate the key parameters in developing the upper bound theorem for optimization of the process. The lubrication effect on the process is also explained using the developed model.

Chapter 3: Experimental Procedures

3.1. Introduction

Based on the literature review discussed in the previous chapter and the gaps identified, a progressive microforming process was modified for production of micro-parts, having relatively fewer challenges compared to previously studied processes, including the handling issue, the process timing, and the material wastage. In this study, micro-pin was selected as a simple feature to be manufactured and studied. However, as a matter of fact, the process has a great capability for production of symmetrical micro-parts in any shape such as hollow pins, stepped pins, or cups [158]. In general, this process has been studied in 3 different main sections of: (i) experimental, (ii) finite element simulation, and (iii) analytical modeling as shown in more details in Fig. 3-1.

Figure 3-1. Flowchart of this study.

In this chapter, the experimental procedures, die-set design, characterization methods, together with simulation and modeling techniques for achieving the desired scope of the project are described.

3.2. Materials and process

3.2.1. Material properties

Electrical Tough Pitch (ETP) C11000 copper (99.94%) strips, in the as-received cold-rolled condition, with a dimension of 200×20×2.5 mm were initially used as the feed for the process. The dimension of the strip selected was based on the primary prototype design of the die-set. Thinner strips with thickness down to 0.5 mm were used to study the grain size and specimen size effect on the process. The initial grain size

of the applied material was $14 \pm 4 \text{ } \mu\text{m}$ excluding twin boundaries (Fig. 3-2). The hardness value of the initial strip was $75 \pm 5.2 \text{ Hv}$.

Figure 3-2. Micro-graph of the microstructure of the copper C11000 used in the process.

3.2.2. Experimental set-up

A schematic of the progressive forming set-up used for production of the micro-pins is given in Fig. 3-3. As illustrated in this figure, the process set-up consisted of two stages: (i) pin forming by forward extrusion and (ii) blanking. In the first stage, the strip is deformed by a punch of a defined diameter and specified displacement. As a result, a portion of the material is forward-extruded into the die orifice. This type of microforming process in a progressive manner has not been investigated nor reported to date.

Figure 3-4 shows the real image of the die-set used in the process, indicating the details of the design. As shown in Fig. 3-4-a, the strip is guided through the die-set via spring ejectors, which hold the strip. These springs push the strip up after pin forming in Stage I, and consequently, eject the formed pin from the die orifice.

Figure 3-3. Schematic of the progressive microforming process indicating the two stages involving in the process: Stage I, Forward Extrusion; and Stage II, Blanking.

After positioning the strip on the die-set, the blank holder (Fig. 3-4-b) is put on the bottom part (Fig. 3-4-a). Then, the spring screws force the blank holder down with a constant blank holder force (Fig. 3-4-c). The blank holder force is applied via springs under the screws. Finally, the assembled die-set is set to the machine under the punches (Fig. 3-4-d).

Caption in the next page ...

Figure 3-4. Real image of the die-set used in the process: (a) Bottom part including the dies; (b) Top part (Blank holder); (c) Assembled die-set; (d) Die-set under the punches.

3.2.3. Forming procedure

The copper strip is inserted via guides on the set-up. To ensure the precise positioning, guide holes are created on the edge of the strip and guide pins are used along the different stages of the die-setup (Fig. 3-4-b). The blank-holder was spring loaded with six springs producing a constant pre-load of about 2.3 kN. The spring-loaded blank holder was used in this study to distinguish the forming load from the blank-holder load. In industrial applications, the blank-holder is usually attached to the press. After extrusion in Stage I, the strip with the attached extruded pin is ejected by springs and can be moved to the next stage.

It is to be noted that the forming process only occurs in Stage I of the process. In other words, Stage II is only used for detaching the pins from the strip. Thus, the formed micro-pin at Stage I is still attached to the strip which made it easier for handling and transferring to the next stage either for blanking or for subsequent measurements. No counter punch was used in this process, which prevent structural pin shape deformation during ejection. The ejecting by the springs on the strip only leads to the withdrawal of the formed pin from the forming die.

In comparison with the micro-extrusion process [11], the introduced process has more capability of mass production as it uses a strip metal as a feed instead of a billet. Moreover, since the formed pins are attached to the strip after the forming process, the handling of the parts

for subsequent characterizations becomes easier compare to the microextrusion process.

It is interesting to note that such a process has a great capability for versatile production of axi-symmetric micro-parts such as hollow pins and step pins, as shown in Fig. 3-5.

Figure 3-5. Macrograph of: (a) pins/stepped-pins; and (b) hollow pins with different diameters manufactured by the progressive microforming process.

3.2.4. Subsequent measurements

Due to the process boundary conditions and material flow in the process, there is a bulge appearing around the formed pins on the strip. Considering the bulge, the method illustrated in Fig. 3-6 was used to

measure the pin height and actual punch displacement, using Eq. 3-1 and Eq. 3-2, respectively.

$$\text{Pin Height} = t_t - t_c \quad (3-1)$$

$$\text{Actual Punch Stroke} = \text{Initial Strip Thickness} - t_b \quad (3-2)$$

As will be explained in the next section, the mechanical behavior of the material can be considered as plastic behavior in the simulation. Thus, to simplify the finite element calculations, the elastic deformation of the punch and the material was neglected.

To measure the plastic deflection in experiments, the actual punch stroke was considered as the punch displacement based on Eq. (3-2), i.e. the head thickness (t_b) was kept constant ($=0.2$ mm) for all pin forming experiments.

Figure 3-6. Measurement method used for measuring the pin height after stage I.

To normalize the pin height and have a better comparison, the pin height value is also presented in the form of the aspect ratio, which is equal to the pin height over the pin diameter. Measurement of the head thickness was only done to ensure the real stroke of the punch on the strip. All the measurements have been done using a micrometer caliper with resolution of 0.001 mm. To ensure the accuracy, all the pin-height measurements were confirmed with the AM7013MT Dino-Lite Premier digital microscope.

The diameter of the second die (blanking die) was the same as the first punch diameter. The diameter of the second punch was 0.1~0.2 mm smaller than the first punch to avoid any damage to the die and punch. The clearance between second die/punch was determined empirically. Before each pin forming, the die and punch surfaces were cleaned and dried using ethanol.

3.3. Forming Press and Tooling

3.3.1. Servo-Press

To conduct the microforming process, a press is required. In comparison with conventional mechanical presses, the servo-press has different advantages, which mainly can be named as: (i) low energy consumption, (ii) advanced control on press movement to avoid the impact loading, and (iii) less break-through and touching noise [159,

160]. In our work as we need accurate control of the press movement, a servo-press was ideally selected.

A programmable servo press (SCHMIDT ServoPress 420) was used for the deformation process (Fig. 3-7). This machine has the ability of operating in different profiles such as Force Control, Stroke Control, and Delta Stroke (Fig. 3-8) [21]. As illustrated in Fig. 3.8, the Stroke control profile, which was used in our experiments, works based on the displacement of the punch as the controlling criteria. This machine can easily plot the Force-Stroke curves during forming with a precise positioning in the range of 0.01 mm. It was also possible to get additional graphical display Force-Time, and Stroke-Time. The die-set design was optimized for the microforming process.

Figure 3-7. SCHMIDT Servopress 420 used for progressive forming of micro-pins.

In the stroke control profile, as illustrated in Fig. 3-8, at first the punch travels down with defined speed to reach a specified position right above the strip surface (PS). Then the speed is reduced, and after the punch reaches the strip surface (IP), the speed again is reduced to the defined strain rate. This

program was done to reduce the risk of breaking the punch and also to reduce the noise.

Figure 3-8. Standard operation profiles in SCHMIDT servopress and their applications. (TDC=Top dead center of the process; PS=Pressing start, start of the process data recording; PP= Probing position; IP=Intermediate position that is required for monitoring purposes; EP=End position) [161].

At this stage, punching is started on the strip. The deformation would continue until the punch reaches to the defined displacement (Punch stroke). The punch will stay at the final position for 3 seconds to make sure about prevention of elastic deflection. After unloading, the strip would be pushed upwards and moves forward to the next stage (blanking).

During blanking of the previously deformed pin, another pin is forming in a progressive manner, and it continued until the end of the strip.

The press unit in the machine was equipped with an electromotor driven spindle. The system was equipped with a mechanical clutch that will separate the motor from the drive in case of an overload. According to the motor speed, the torque is produced, which is regulated load-dependent. The force is a function of current. This sine commutation, in connection with the integrated resolver, provides the exact and absolute rotational position of the rotor, relative to the rotating field. Accordingly, the system knows the exact ram position at any time [162].

Load-displacement of the process can be monitored by the machine precisely with maximum 1% error. The maximum forming force was measured with resolution of 0.01 kN.

3.3.2. Tooling

Dies with a hole-diameter between 0.3 to 2.5 mm, were manufactured by precise electro-discharge machining method, using similar machining

conditions. The dies and punches were manufactured from SKD11 steel material, with diameter tolerance of ± 0.005 mm. The same method was used for production of punches with diameters between 1.2 to 5 mm, as shown in Fig. 3-9. In this research the punch geometry was kept constant and only the punch diameter changed. The punch speed was kept constant during plastic deformation, (0.1 mm/s) which was chosen based on previous works on micro-pin forming [10, 14]. This deformation speed is good to minimize the effect of strain-rate sensitivity of the metal.

Figure 3-9. Punch and dies with different diameters used in this study for micro-pin forming.

The peak-load of the process and the pin height was selected as two important factors to check the reproducibility of the experiments. For all cases, the maximum scatter in results for both factors was less than 10%, which demonstrated a narrow experimental variation.

3.4. Experimental plan

3.4.1. Die and punch diameters

Different die diameters between 0.3 to 0.8 mm were used to investigate the effect of die diameter on micro-forming behavior of material. To have a comparison between micro and macro scale, 1.2 mm die was also utilized.

Moreover, by changing the grain size, the number of grains over the diameter of the pin can be changed accordingly. So, by choosing different die diameters and changing the grain size, various T/D ratios can be studied.

Furthermore, punches with diameters of 1.2, 2.0, 3.2 and 5.0 mm were selected to investigate the effect of punch/pin diameter ratio on the material's behavior.

At first, a constant punch/pin diameter ratio of 4 was considered for all pin forming processes to eliminate the effect of punch diameter on the process. Therefore, punches with diameter of 1.2, 2.0 and 3.2 mm were used for the pins with diameter of 0.3, 0.5 and 0.8 mm, respectively. A list of pin and punch diameters used in this work with their ratio is presented in Table 3-1.

In all cases, the stroke was the target as the operating profile for the machine. For the thick strip (with thickness of 2.5 mm), various strokes of 1.0, 1.5, 2.0 and 2.3 mm were selected to study the microstructure evolution during the process. In case of thinner strips, the maximum punch displacement was calculated according to the constant head thickness of 0.2 mm (see Fig. 3-3).

Table 3-1. Punch/Die diameter ratios used in this study.

Punch Dia. (mm)	Pin Dia. (mm)	Punch/Die diameter ratios			
		5.0	3.2	2.0	1.2
0.8		6.25	4	2.5	1.5
0.5		10	6.4	4	2.4
0.3		16.7	10.7	6.7	4

3.4.2. Lubrication

To investigate the effects of lubrication conditions on microforming, two different liquid lubricants with different viscosities were used as compared to the dry condition: (i) WISURA Z0 3373 (Oil, High viscosity 140 cSt), (ii) FUCHS Ecocool (Emulsion type, 10% in water, Low viscosity). A normal machine oil was used also, which was not effective. The Teflon layers with thickness of 0.1 mm were also used as a solid lubricant. The challenge was how to ensure that the lubricant flows inside the die orifice, as the die orifice was very small. To resolve this issue, the dies were dip-drawn in the liquid lubricant using ultrasonic vibration to help the liquid to flow better inside the die orifice.

In case of solid lubricant (e.g. coating) as checked technically with relevant companies, it is impossible to coat an efficient solid lubricant layer inside the entire micro-die orifice thoroughly. The only solution is to use the segmented die and coat inside the die orifice. However, this type of die does not provide the required accuracy for such a process.

The friction coefficient of each condition was determined by ring compression test and FE-simulation [103, 163]. The ring dimensions used in this study is illustrated in Fig. 3-10.

All the experiments were done at least three times to assure the reproducibility of the results.

Figure 3-10. Schematic of the ring used for ring compression test.

3.4.3. Annealing

To study the grain size effects, the specimens were annealed at different temperatures of 400, 600 and 800 °C in a vacuum tube furnace to prevent oxidization of copper strips. The temperature rate was kept constant at 3 °C/min and the dwell time was 1 hour for each temperature. Increasing the dwell time would not change the grain size significantly.

To study the effect of substructure dimension on the post-production material behavior, a batch of micro-pins was recovery-annealed. Recovery is an annealing phenomenon occurring prior to recrystallization. The structure of the dislocation network changes by recovery. The percentage of this change depends on the stacking fault energy of metal [164]. Since copper has a relatively medium stacking fault energy, recovery is possible to occur in the microstructure before recrystallization, if a good control of annealing temperature is considered [165]. During recovery, dislocation annihilation within dislocation cells and also grain boundaries, coalescence, and growth of subgrains are the main phenomena occurring in the microstructure of the metal. Subgrain growth rate depends on the mobility of subgrain boundaries and its misorientation angles [166]. Recovery does not remove all the substructure and just stabilize subgrain structure without having any significant effects on the grain size [167].

Although it is difficult to control the recovery process without having any recrystallization in medium or high stacking fault energy metals, a temperature of 230 °C was selected for 1 hour as the recovery annealing cycle for the copper [167]. This was done in the same Lenton vacuum tube furnace under vacuum of 10⁻⁵ mbar using a ramp of 10 °C/min and then cooled in the air. The recovery-annealed micro-pins were named as RA micro-pins in this study.

3.5. Characterization Techniques

3.5.1. Surface roughness observation

Before microstructural observations, the surface roughness of the pins and the strip metal was examined using a non-contact surface profilometer. The Alicon InfiniteFocus is an optical 3D micro coordinate system for form and roughness measurement with a vertical resolution of 10 nm. The non-contact nature of this device made us able to measure the roughness in the round surface (radial direction of the pins).

After optical calibration of the device, the pins were horizontally stuck on a white paper for optical investigation. The device has a microscope which takes photos from deeper part of the sample to the tip. Thereafter, by image analysis of these pictures, the roughness values will be given.

Two types of roughness value were shown by 3D profile-meter, which are:

- Average roughness of profile, R_a
- Mean peak to valley height of roughness profile, R_z

Figure 3-11. Directions for measuring the surface roughness.

For the micro-pins, the mean value of three measurements of each R_a and R_z were reported in two directions, taken with reference to the micro-pin, as shown in Fig. 3-11, which are:

- The roughness values in micro-pin's radial direction, x
- The roughness values in micro-pin's axial direction, y

The cut-off value for each test was selected based on ISO 4288-1996 [168].

3.5.2. Microstructure observation

Field Emission Scanning Electron Microscope (FESEM, JEOL JSM-7600F) was used for surface quality observations. This microscope was also used for Electron Backscatter Diffraction (EBSD) analysis.

3.5.2.1. Sample preparation

One of the most important challenges in this work was to observe the microstructure at half cross-section of the micro-pins. Since the pins diameters were so small, cutting, grinding and polishing of the pins was rather a difficult task. One way to get over this challenge is to mount the pins vertically in the usual round mounts and then cut the mount into two sections in radius direction. By grinding and polishing the half mount, the cross-section of the pin can be observed (Fig. 3-12). Both hot and cold mounting can be used for this purpose. Etchant solution was 10 g Ammonia Persulfate (Merck) in 100 ml distilled

water. Etching time was about 4 min. Alternatively, 2 ml H_2O_2 , 25 ml HNO_3 , in 65 ml H_2O can be used as the etchant to see the grain boundaries and twins. The etchant time for the latter solution is less than 30 seconds. However, the former method provides better images in the case of flow patterns in the material. The Olympus optical microscope was used for cross-section microstructural observations. To measure the grain size, the ImageTool software version 3.00 was used.

Figure 3-12. (a) Schematic; and (b) Real image of the sample preparation ready for polishing and microstructural observation.

For the EBSD analysis, after mechanical polishing, the samples were electropolished in a 17.5 mL distilled water, 82.5 mL Phosphoric acid (85%) solution, using cell voltage of 1.5 V for 15 mins. Pure copper was used as the cathode.

3.5.2.2. EBSD characterization

The FESEM was conducted with an Oxford Instrument HKL EBSD system working at 20 kV with a working distance of 28 mm and a 70° tilt angle. The CHANNEL 5 suite of programs developed by Oxford Instruments was adopted to manipulate, analyze the EBSD data in this study.

For the microstructure determination, individual specimen locations were investigated using step-wise scanning technique with the step size of 0.2 μm to be able to detect small subgrain/dislocation cell size with the least error. The lower step sizes did not change the results accuracy, significantly. For the data sets obtained from EBSD mapping, all the fractions of successfully indexed data pixels were higher than 70%, which is reliable for determining the crystallographic texture [169].

In the high-resolution EBSD, high angle grain boundaries (HAGBs; misorientation > 15°) were depicted as black lines. In order to avoid spurious boundaries, misorientations below 2° were not taken into account. It is worth noting that although most of the researchers consider the upper limit of 15° as for the LAGBs, but some consider the

10° as a threshold misorientation [170]. To be more conservative, misorientations between 2 to 10 degrees were considered as LAGBs. The boundaries with misorientation of between 10 to 15 degrees were also presented for comparison. Grain size measurement was carried out in the EBSD software by determining the grain areas exclusive of border grains. The mean values of the subgrain/grain size of the microstructures were obtained from the statistical evaluation tool embedded in the EBSD software. The same route has been used before [170-173].

3.5.3. Mechanical testing

3.5.3.1. Mechanical properties of the initial samples

The upsetting test is used to study the deformation behavior of material having different grain sizes. The specimens for this test were a cylinder with 1.67 mm in diameter and height of 2.5 mm. The electro-discharge machining was used to cut the specimens for upsetting. The samples were cut in normal direction to the strip surfaces to keep the upsetting conditions similar to the microforming conditions. The upsetting tests were conducted at least three times on the specimens with punch speed of 0.1 mm/s.

Two layers of Teflon, with thickness of 0.1 mm, were used as the lubricant on the top and bottom surface of the specimens. To eliminate the friction effects

on the upsetting test results, the tests were stopped at every 10% reduction to renew the lubricant layer.

3.5.3.2. Mechanical properties of the formed pins

To have an idea about the mechanical properties of the pins, microhardness tests were conducted using MTS Vickers Microhardness tester on the half cross-section of pins. It is important to note that the microhardness load must be selected in a “load-independent” range. Indeed, if the indentation load is too high or too low, the reported microhardness value will not be accurate. Therefore, usually an indentation load sensitivity test is done to have an idea about the indentation load [58, 174]. This test showed that the indentation load of 0.5 N provides an accurate result for the copper alloy with the least error.

To visualize the microhardness profile, the Surfer software version 8.00 was used. This software is capable of creating contour maps for different purposes.

In addition, micro-compression test was designed to be conducted on the formed pins to compare the stress-strain curves. There has been reports trying to get the mechanical response of the micro-pins or micro-pillars via compression test [131, 175]. The main challenge in micro-compression tests was holding the micro-cylinder during the test. Zhang *et al.* [131] used finite element simulation to suggest the usage of a base attached to the pins for stabilizing the pins under the indenter. As reported [131], the base material does not participate in deformation. Furthermore, friction has a very negligible effect on the deformation

behavior in this case ($<0.5\%$). The proposed feature is exactly similar to the final product in the progressive microforming process. Therefore, the manufactured micro-pins can be directly used for studying the post-production size effects via micro-compression test without any required machining (Fig. 3-13).

Figure 3-13. Schematic of the micro-compression test.

Zhang *et al.* [131] also studied the effects of the pin aspect ratio (pin length to its diameter) on the deformation behavior in micro-compression. To be in a safe region in case of buckling, a constant aspect ratio of 2.5 was chosen for our micro-pins. The aspect ratio of the micro-pins can be easily controlled in the progressive microforming process.

The same servo-press with a flat-end indenter was used as a press for the compression test. The achieved data presented as an average of at least three micro-compression tests.

To ensure the accuracy of the results obtained from the micro-compression tests, a visual inspection was done in each sample after the test. As shown in Fig. 3-14, buckling/tilting can occur due to inaccurate positioning of the pins under the punch.

Figure 3-14. Compressed micro-pin: (a) without buckling-uniformly deformed; (b) with buckling-faulty.

The center of the pin must be exactly matched with the center of the punch, which was done empirically. Zhang *et al.* [131] also mentioned that no lubricant must be used on the pin tip to prevent buckling or tilting. A 0.1 mm thick Teflon layer was, however, used under the base (head part) to eliminate the extra friction effects on the mechanical response.

It was reported that the tip curvature of the micro-pins can lead to pin buckling during the compression. To solve this issue a mold made of Poly-Methyl-Methacrylate (PMMA) was designed to hold the micro-pins (Fig. 3-15). PMMA is a transparent thermoplastic, often used as a lightweight or shatter-resistant alternative to glass. The hole with a proper diameter (equal to pin diameter) was made by laser. Then the pin was inserted into it to be held during

the process. Then using a precise machining or even using a fine grinding paper, the pins' tip was flattened.

Figure 3-15. Flattening of the micro-pins' tip using a polymeric fixture.

3.6. Finite Element Simulation

3.6.1. Process design

DEFORM-2D™ (Version 10.1.2) was used for the process simulation. Due to the symmetry of the process, only half cross-section of the material in the process was simulated using cylindrical coordinated in axisymmetric condition (indicated X for radial direction and Y for height) (Fig. 3-16).

3.6.2. Boundary conditions

As shown in Fig. 3-16, there are four objects in the simulation: the moving punch, the die with a defined orifice diameter, the strip, and the blank holder. Except for the strip, all other objects were considered as rigid bodies. The 4-node quad element was used to mesh the strip.

Since the strip width is much longer than the pin diameter, the material width was assumed infinite. However, to make the simulation conditions much closer to the real conditions, the material movement in horizontal (X) direction was considered zero at the end of the strip.

By running the simulation, the punch moves down in the $-Y$ direction and deforms the strip into the die orifice. The blank holder was movable in $+Y$ direction, pre-loaded with constant force of 2.3 kN in $-Y$ direction and fully fixed in the $+X$ direction. The value of the pre-loaded force was determined based on the maximum displacement of the springs for the blank holder.

Figure 3-16. Die-set design used in simulation

3.6.3. Meshing / Re-meshing

Two mesh windows were used for the deforming strip; one with fine mesh size of 0.05 μm for the portion of the strip in the punching area; and one with a coarser mesh size of 5 μm , for the rest of the strip to reduce the simulation time.

After setting the conditions, re-meshing sensitivity was done for various re-meshing criteria values, and based on that, the re-meshing criterion was set to either every 0.5s or every 0.05 mm punch displacement. Decreasing the re-meshing criteria under the mentioned values only increased the computing time, without causing any significant effect on the accuracy of the results.

3.6.4. Data input

The average of the 3 upsetting test results was used as the stress-strain behavior of the copper strip in the simulation. The point-to-point data imported to the DEFORM-2D software, and it was automatically extrapolated (as it was with the same slope) to higher strains without using any model. To ensure the accuracy of the imported data, the values of the mechanical properties was calculated using the fit method in the DEFORM-2D software: Yield strength of 240 MPa, Elastic modulus of 117 GPa, and Poisson's ratio of 0.34. These obtained data were perfectly matched to the range reported for the C11000 copper alloy in ASM handbook [176].

3.6.5. Laser Marking; Grid-lines and Strain Calculation

To ensure the validity of the simulation results, the amount of shear strain was calculated experimentally using grid marking on the cross-section of the strip. To do so, the wire-cut method was used to cut the strip precisely into two equal sections with a smooth cutting interface. The Nd-YAG laser was used to etch the cross-sectional surface of the copper strips as shown in Fig. 3-17.

Figure 3-17. Schematic of: (a) the cross-sectional laser marking, and (b) welding of the copper strip.

A grid with grooves width and depth of 15 μm was made on the cross-sectional surface. A few fine spot welding were used to weld-back the copper strip together. The location of spot weld was chosen far away from the forming punching location on the strip to prevent any heating effects on the grain structure.

The produced pin was then a segmented pin, which could be separated from the middle to see the shape-change of the markings at the interface of the strips (Fig. 3-18).

Figure 3-18. Schematic of the manufactured micro-pin. The pin was segmented from the separation line to see the laser marks after forming.

The grid line in the DEFORM software was built with the grids' distance of 0.03 mm. This way, there would be enough grid elements over the diameter of pin to be studied.

After pin forming, the marked grid-line was distorted due to the material flow in the process. The value of the strain was calculated based on the theory of plasticity. It is known that the effective strain can be calculated from the grids' distortion as shown in Fig. 3-19 and Eqs. 3-3 and 3-4.

The summation of the tangent of angles α and β determines the total shear strain that has been applied to the element. Based on Fig. 3-19, the individual shear strain in each direction can be defined as:

$$\begin{aligned}\gamma_x &= \partial u_x / \partial x \\ \gamma_y &= \partial u_y / \partial y\end{aligned}\quad (3-3)$$

The total shear strain is the sum of the two shear strains as

$$\gamma_{xy} = \gamma_{yx} = \frac{\partial u_x}{\partial y} + \frac{\partial u_y}{\partial x} \quad (3-4)$$

Figure 3-19. Two-dimensional deformation of a material element.

3.6.6. Friction in simulation

The other challenging factor in simulation of the microforming processes was the friction factor [19]. At first, friction conditions were assumed the same on the strip-die, strip-punch, and strip-blank holder interfaces (global friction). The reverse engineering method was used to fit the experimental results by adjusting the friction factor for the simulation, using pin height and the forming force as key values.

As mentioned by Huang *et al.* [177], under high friction coefficient (sliding together with high normal pressure between interfaces), or under some geometrical conditions, the interface friction must be considered as hybrid friction. In this condition, based on the amount of the normal pressure on the interfaces, the friction will be considered locally as either Coulomb or Shear friction, which will be discussed more in the next chapter. This procedure is done automatically in the DEFORM software based on the normal pressure on the interfaces. This method was done for all pin and punch dimensions.

It is to be noted that in some reports it has been mentioned that conventional finite element models cannot directly be applied in micro-scale forming processes [3, 178]. In this case, Cao *et al.* [178] introduced a new Voronoi Grain (VGRAIN) system to develop crystal plasticity theories for micro-mechanics. It was reported that by integrating the VGRAIN system to the conventional models, and considering the microstructure and grain orientations, the material behavior in micro-forming processes can be predicted easily.

However, this finding is valid when the T/D value (the number of grains over the cross-section of the workpiece) goes below the critical range, which causes the size effects. In this study, as there are several grains over the thickness of the 2.5 mm thick strip, there was no significant size effect seen. Therefore, the classic finite element modeling could be still considered valid for this work.

3.7. Summary

The procedures for experimental studies, finite element simulation, and analytical modeling of the progressive microforming process were described.

The die-set design with its details and various forming stages during the process were explained. After pin forming, the dimensional measurement techniques used in this study were described visually. A servo-press machine used to be able to control the punch speed and displacement accurately in different stages of the process. Micro-pins with different diameters from 0.3 to 0.8 mm were manufactured. To study the effect of lubricants, two different liquid lubricants with different viscosities were used. Teflon layers with thickness of 0.1 mm were also used as the solid lubricant. Due to the very small dimension of the die cavities, it was impossible to coat inside the die orifice. The ring compression test was used to determine the friction coefficient of the lubricants. The procedure for sample preparation for microstructural observations was explained. The dimensional measurements of the pins, together with the forming load were recorded during the process. Micro-compression test together with microhardness test were used to evaluate the mechanical properties of the formed pins.

The boundary conditions for finite element simulation and analytical modeling were explained. The simulation was used to have better understanding about the material behavior during the process. Analytical modeling was useful to get a universal solution for the process optimization.

Chapter 4: Experimental Study and Finite Element Simulation of the Material Flow

4.1. Introduction

As suggested by Hirota [10] a strip metal can be used as a feed for the extrusion process to circumvent the handling issues of micro-pin forming processes. Estimated flow patterns showed that the degree of material flow occurring inside the shear zone contributed to the pin height. Besides the shear zone usually there is a dead metal zone (DMZ), which has weaker mechanical properties compared to the shear zone. Investigation of the behavior of the DMZ during the process can be a great help in understanding the process deformation mechanism.

In literature, there is also a lack of detailed studies on microstructural evolution of micro-parts during microforming process and none in the area of progressive microforming [10, 11, 13-15]. There is also a very limited understanding on the microstructure-properties relationship in final micro-

products. In addition, investigating the microstructure of the formed pin could be useful for understanding the material flow during the process.

To optimize the process, one needs to understand the process conditions, the effect of process parameters as well as the material flow behavior to optimize the process. Finite element (FE) simulation is a tool that can assist greatly in this effort. However, FE simulation of microforming processes has always been a challenge due to different types of size effects occurring in such length scale [21, 55, 102, 136, 137, 179]. One such challenge is to define the correct friction factor, so that FE simulation results can match the experimental results.

4.1.1. Scope of this chapter

In this chapter, the feasibility of producing micro-parts from a strip using the progressive forging/extrusion process will be shown. The material flow behavior during the forming process and the microstructure of the formed copper micro-pins are studied. The effect of different process conditions (die diameter, die design and punch diameter), on the respective material flow behavior is elaborated. The importance of the location of the neutral plane on the material flow is investigated using FE simulation. Combining the experimental with the simulation results, it is shown how to control the material flow during the process. It is also used to study the friction factor effects. The as-received copper strips with the constant average grain size of $14\pm4\text{ }\mu\text{m}$ were used in this chapter.

4.2. Progressively formed micro-pins

Micro-pins of diameters between 0.3 to 2.5 mm were produced successfully using the open-die micro-forging/extrusion process (or so-called progressive microforming process). After the forming process, no significant damage in terms of pin fracture and buckling was observed in the experiments, as can be seen from Fig. 4-1 and Fig. 4-2. This was indicative that the ejection process of the formed micro-pin by the ejecting springs on the strips does not cause any damage on the formed pin in this process. Nevertheless, the galling effect on the pins' surface was observed, which will be reported in next sections.

After the blanking (Stage II in the progressive forming process), the outcome is the micro-pin attached to a disk-shape head (Fig. 4-1-b).

As shown in Fig. 4-2, there is a bulge around the formed pin after Stage I. The formation of the bulge is attributed to the lateral material flow caused by the openness nature of the process (no horizontal constraint). The bulge did not lead to any problem in transportation of the strip to the different stages of the progressive die. There was minimal distortion of the strip since the bulge height was found to be less than 0.5 mm. The same bulging behavior has been reported in micro-pin forming process by Hirota [10] for his vertically unconstrained case.

It is worth noting that as the progressive process is conducted on a metal strip, it is impossible to make the process fully constrained. Nevertheless, for the current set-up, the vertical constrain can be further improved by clamping of the blank holder. Compared to Hirota's work [16], making the "vertical

constraining condition” in this process possibly could result in between two to three times increase in the pin aspect ratio.

Figure 4-1. Formed micro-pins, (a) Before blanking stage; (b) After blanking stage.

Figure 4-2. Bulging around the formed micropin, due to the lateral flow of the material.

4.2.1. Surface roughness

Figure 4-3, illustrates a typical result obtained for the roughness investigation of the 0.8 mm pin manufactured by 3.2 mm punch. These data are achievable after optical calibrations.

Figure 4-3. Typical result achievable from the roughness optical profilometer; for the 0.8 mm pin manufactured by 3.2 mm punch in axial direction: (a) roughness profile; (b) roughness values.

Figure 4-4. Surface roughness values for different pin diameters manufactured by various punch diameters.

The measured mean values of R_z and R_a did not differ much for different pin diameters for both the x and y directions (Fig. 4-4). Since the dies were manufactured using the same EDM process, the surface micro-roughness texture is the same inside the die orifice. In addition, the nature of the process causes the same roughness texture on the pins' surface when the pins are ejected from the die orifice. Consequently, there was no roughness size effects observed in this process.

It is worth noting that for the dies and punches the average value of the R_a and R_z was $453 \pm 16 \mu\text{m}$ and $467 \pm 28 \mu\text{m}$, respectively. The measured mean values of R_a and R_z for the initial copper strip were $233 \pm 26 \mu\text{m}$ and $359 \pm 7 \mu\text{m}$, respectively.

As mentioned, the galling effect can be clearly seen on the surface of the pin, as shown in Fig. 4-5-a and Fig. 4-5-b. It suggests that there is a copper-copper contact inside the die (due to galling) during pin forming, which makes the friction condition severe. This challenge has been reported before [114, 134] which needs more investigations to be understood.

Figure 4-5. Surface image of the 0.3 mm pins manufactured under the condition of: (a) dry; (b) lubricated using FUCHS lubricant.

The main solutions for galling in metal forming are either coating of the die surface or using lubricants [180]. However, due to the

miniaturized size of the die diameter, it is almost impossible to coat inside the die orifice.

The application of lubricant was found to have little effect on the pin surface (Fig. 4-5-b). This was attributed to the lubricant being squeezed out during the forming process by the metal flow inside the die orifice. Therefore, there was no lubricant to be trapped in the surface micro-cavities to prevent the galling.

It is worth noting that applying lubricant has limited effect on the pin height and forming load. This fact will be further discussed in the last chapter.

4.3. Dimensional investigations: Effects of Punch-to-Pin diameter ratio on the pin height

Increasing the die diameter from 0.3 to 0.5 and 0.8 mm allows the material to flow more towards the die orifice. This leads to an increase in the pin height. However, in reporting the pin aspect ratio, there is no change observed, as shown in Table 4-1. As can be seen, using a constant punch-to-pin diameter ratio leads to a constant pin aspect ratio. Indeed, changing the punch/pin diameter ratio leads to change in the pin aspect ratio (Fig. 4-6).

Table 4-1. Micro-pin aspect ratio for different pin diameters (Experimental results).

Punch diameter (mm)	Pin diameter (mm)	Punch/Pin ratio	Pin Aspect-Ratio	Maximum Forming Load (kN)
1.2	0.3	4	5.2 ± 0.1	3.3 ± 0.2
2.0	0.5	4	5.3 ± 0.1	14.5 ± 0.6
3.2	0.8	4	5.2 ± 0.1	23.7 ± 1.2

Figure 4-6. Effect of punch diameter on pin aspect ratio and forming stress in the progressive microforming process. Pin diameter: 0.3 mm.

As shown in Fig. 4-6, increasing the Punch-to-Pin diameter ratio leads to higher pin aspect ratios. Furthermore, the required forming load (or forming stress) was also increased by increasing the Punch/Pin diameter ratio. This is logical as by increasing the punch-to-pin diameter ratio, there would be more material under the punch being able to flow towards the die orifice and form the pin. Therefore, the

optimum punch diameter must be selected based on the required applications and forming load available.

The punch-to-pin diameter ratio also interestingly affects the material flow pattern and consequently, the microstructure of the pins formed, where appearance of a soft-zone on the pin surface was observed. This fact will be elaborated more in the future sections.

Decreasing the die diameter from 0.8 to 0.3 mm resulted in an increase in the forming load, as the bulk of the load is due to the upsetting phenomenon (Fig. 4-6). In other words, it is inferred that decreasing the die diameter makes the process very similar to the upsetting/embossing test with a relatively constant forming load in our process and set-up.

4.3.1. Punch Load-Displacement behavior; Phenomenological study

Figure 4-7 shows a typical load-stroke curve for the progressive microforming process. Almost a similar behavior was observed in the punch reaction of similar processes, in which the first stage was contributed to the elasticity, and the last two stages in the curve was related to the plastic behavior of material in the process [3, 25].

At the first stage, the forming load is relatively low, which corresponds to the elastic forming initiation and indentation process. The portion of this stage is relatively small. At Stage II, upsetting (compression) is the main phenomenon, mainly due to the less force required for this phenomenon compare to that of the extrusion. Till this stage, the material mainly flows

towards the outside of the punching area rather than moving towards the die orifice.

Figure 4-7. Load-Stroke curve for the 0.8 mm pin produced by 3.2 mm punch.

After reaching Stage III, the load increases rapidly. In this stage, the required extrusion force is less than that of upsetting and therefore, the extrusion becomes the dominant phenomenon occurring in the process. As a result, the metal will mostly flow towards the die orifice rather than flowing outwards. The unloading portion of the curve presents the elastic deflection of the material and punch, essentially due to the force in Stage III. This shape of the load-stroke curve is similar to what occurs in the common impression-die forging process [181] as reported also for bending process [3].

4.3.2. Material flow

The simulated material flow of pin forming is shown in Fig. 4-8. The material velocity vectors show the flow direction in these images. These figures show that at the very beginning of the process, there is almost no pin formed inside the die orifice, although the punch stroke is relatively large (1 mm). Therefore, until this stage, there is no “pin-forming” and material only flows outwards, as can be seen by the velocity vectors’ directions towards Y-direction. This material flow causes the strip to start to bulge. Before this stage, the material was only indented by the punch.

From the stroke of 1.5 mm to around 2 mm, the material mainly flows sideways similar to the compression test, causing a bulging around the punching area on the strip. The main bulging of the strip occurs in this stage.

As a result, in the second stage, the main physical phenomenon is compression (upsetting test) taking place in the process. After certain punch travel (stroke of around 2 mm), the bulging almost stops and therefore, the material only flows towards the die orifice. This effect can be seen also in Fig. 4-7, which shows a rapid increase in the pin height after stroke of around 2 mm. Thereafter, this stroke, extrusion is the main phenomenon taking place in the process. This is mainly due to the fact that the flow of material outwards will be more difficult at the end stroke, since the upsetting will be more difficult at this stage.

This again indicates that there is a critical strip thickness, after which the main pin-forming process occurred. Having the critical thickness, the process can be optimized using less amount of material, i.e. thinner sheet metal. Furthermore, the concept of production of arrays of micro-pins in one punch stroke, instead of just a single micro-pin can be considered, making the material waste even less in the process.

Figure 4-8. Simulation results of the pin forming process for the 0.5 mm pin manufactured by 2.0 mm punch, showing the velocity vectors at different punch displacements of: (a) 0.5 mm, (b) 1.0 mm, (c) 1.5 mm, (d) 2.0 mm, (e) 2.1 mm, (f) 2.2 mm.

It is worth mentioning that a similar material behavior in terms of final product dimension and also microstructure was observed for the pins manufactured from a pure Aluminum strip.

In the next section, the mentioned stages will be shown on the microstructure development of a typical micro-pin.

4.4. Microscopic Investigation; Material flow and microstructure characterization

4.4.1. Typical microstructure of the formed pins

In Fig. 4-9 different stages of the process have been identified in the microstructure of a typical 0.8 mm pin. As can be seen, the major structure of the pin is formed during the extrusion process. The magnified images show the microstructure difference between various stages. The tip of the pin was just pushed inside the die orifice during Stage I (indentation). Since there was no deformation (shear or elongation) applied to this part of the metal, the grains look like equi-axed grains.

The next section of the microstructure contains more elongated grains, formed during Stage II (upsetting). The central grains, however, are less deformed and look more like equi-axed ones. The reason is that in this stage, shear deformation mechanisms are still not activated, since the upsetting phenomenon is dominant.

However, the grains formed during Stage III (extrusion) are more elongated and deformed. Indeed, during this stage, grains undergo shear deformation before entering the die orifice.

Figure 4-9. Typical microstructure of the 0.8 mm pin showing different stages (with their relative percentage) involving in the process.

The final product of the process is a micro-pin attached to a “head” part. The material is flown from the head part towards the die orifice and forms the micro-pin; hence the material flow in the “head” part is important. The FESEM micrograph of the head part of the 0.8 mm pin manufactured by 3.2 mm punch

illustrates the material flow in the head part (Fig. 4-10). Generally, two distinguished grain structures are seen in this figure: (i) highly sheared grains in the shear zone, and (ii) less-deformed grains at the workpiece-die and workpiece-punch interface.

Figure 4-10. FESEM micrograph of the head part in manufacturing of the 0.8 mm pin with 3.2 mm punch.

4.4.2. Dead Metal Zone (DMZ)

Figure 4-11 shows typical microstructure of the 0.8 mm pin with fillet radius of around 0.4 mm (0.8^W) manufactured by the punch diameter of 3.2 mm. As illustrated in Fig. 4-11, there are typically three zones in the microstructure which are less deformed, designated “dead metal zones” (DMZs). The DMZs in the microstructure consist of a triangle-like zone at the bottom of the head part,

just beneath the punch surface (DMZ1), and two zones at the top of the head, on the die surface (DMZ2, DMZ3). Besides the identical microstructure, the main characteristic of these zones is the lower hardness values (less than 70% of the maximum hardness value [182]). The material outside of these dead zones was severely deformed, which can be identified by sheared grains in Fig. 4-11. Despite the sheared zones, there is no clear flow pattern in the DMZs.

Figure 4-11. Typical microstructure of cross-section of the micro-pin with diameter of 0.8^w mm, punch diameter of 3.2, and punch stroke of 2.3 mm. Dashed lined zones represent the so called “dead metal zones” (DMZ).

The behavior and shape of the DMZs in this process are relatively similar to what is seen in upsetting test [183]. The only difference is the die orifice under

the punch which is not present in upsetting test. Thus, there is only one triangular DMZ under the punch (DMZ1). The other two on the die surface (DMZ2, DMZ3) are separated by the die orifice. The left border of DMZ3 and right border of DMZ2 are just consequence of the shear-off after blanking stage. It is worth noting that the DMZ2 and DMZ3 are technically the same, due to axi-symmetry of the process.

A very interesting phenomenon was seeing a DMZ layer at the pin surface. In ordinary extrusion work, there is a sheared region at the edge which is due to friction at the die surface, as also reported by Parasiz *et al.* [14]. However, this was not observed in the pin formed by the progressive microforming. This behavior is mainly dependent on the process geometrical conditions.

4.4.2.1. Effect of punch diameter on the DMZ behavior

To prove the existence of such a “soft layer” at the pin surface, the microhardness test was done on the cross-section of the pins (Fig. 4-12-a to Fig. 4-12-h). In all the figures, the microhardness data corroborated the microstructures observation using the image-processing software called Surfer Version 8.00. The DMZs are shown with light gray color in the microhardness profiles, and also by dash-lined regions in the microstructure. Comparatively, in the cases of pins without DMZs at the pin surface, there was almost no difference between hardness values of the surface and center of the pin indicated pins having more uniform mechanical properties.

The grains inside the DMZs are equi-axed grains and essentially un-sheared. The grains in the deformation zones are heavily deformed and elongated in the direction of the material flow. This difference in grain shape is due to the non-homogeneous plastic deformation in the microforming process with hard and soft regions. As a consequence, it is important to have a good understanding about the origin and development of the “DMZs” in the microstructure during the progressive microforming process.

Caption in the next page ...

Figure 4-12. Microstructure and microhardness profile (Hv) of the cross section of the different pin diameters with different punch diameters; 3.2 mm Punch with different diameters: (a) 0.8^w mm, (b) 0.8 mm, (c) 0.5 mm, (d) 0.3 mm; 2.0 mm Punch with different diameters: (e) 0.8^w mm, (f) 0.8 mm, (g) 0.5 mm, (h) 0.3 mm.

4.4.2.2. Development of the DMZ during the process

To have a deeper understanding about this fact, the process parameters (i.e. punch and die diameters) for two micro-pins, one with and one without DMZ on the pin surface were selected (Fig. 4-12-d and Fig. 4-12-h). The microstructure evolution during the forming process was studied at difference

punch strokes. Figure 4-13 shows the evolution of the microstructure of the 0.3 mm pin manufactured by 3.2 mm punch (3.2 mm in diameter).

As can be seen, at first the DMZ forms at the interfaces. In fact, due to friction, a layer of the material on the interface becomes relatively stationary during the process. The DMZ fractional volume increases by continuing the microforming (increasing the punch displacement). In this fashion, after a while, the two separated DMZs under the punch move closer towards each other (Fig. 4-13-b). Finally the two DMZs meet, and it leads to merging of the two DMZs. At the end stroke, the material flow in the shear zones pushed the material in the merged DMZ towards the die orifice. This is named DMZ leakage.

As will be explained in the next section, this was mainly due to the relatively high ratio of punch-to-die diameter, which initiated the material-flow to push the DMZ under the punch towards the die orifice. In comparison, for the 0.3 mm pin manufactured by the 2.0 mm punch, at a very first stroke the microstructure is similar to the one manufactured by 3.2 mm punch (see Fig. 4-13-a and Fig 4-14-a). However, since the punch diameter is relatively smaller, the two DMZs on the head part would not merge. As a result, no DMZ at the surface of the final pin was observed for this pin (Fig. 4-14-c).

Figure 4-13. Micrographs/Microhardness profile of the 0.3 mm pin manufactured by the punch diameter of 3.2 mm in different strokes of: (a) 1.5; (b) 2; (c) 2.3 mm. Dashed regions show the DMZs.

Thus, it can be concluded that the plastic flow under this condition is such that a wider punch creates bigger DMZs that may be pushed into the die orifice. However, by using the smaller punch, the force and the severity of material flow under the punch will be decreased.

Physical mechanism of the formation DMZ can be better and further understood via finite element simulation, which is the title of the next section.

4.5. Finite Element (FE) Simulation; Location of the neutral plane

4.5.1. Neutral plane - definition

As the current progressive process was not constrained, there was material flow in the head region in two directions, i.e. towards the die orifice and away. This leads to a “neutral plane” in the head part, which separates the two material flow directions. As will be explained in greater detail in the next chapter, the neutral plane moves during the process to minimize the energy required for forming. Due to its dynamic nature, the neutral plane cannot be observed distinctly in the optical micrographs, and FE simulation work was used to identify the location of the neutral plane during the forming process.

Figure 4-14. Micrographs/hardness profile of the 0.3 mm pin manufactured by the punch diameter of 2.0 mm in different punch displacements of (a) 1.5; (b) 2; (c) 2.3 mm. Dashed regions show the DMZs.

Identifying the location of the neutral plane will assist in the understanding of the development of DMZ in the micro-pin. In addition, the location of the neutral plane is also important as it will determine the volume of active material that goes into the die orifice to form the pin, directly affecting the final pin height.

After setting the correct boundary conditions on the finite element simulation software, the maximum forming force obtained from simulation was comparable to the experimental force, with less than about 10% error, which is in a well-matched range. Such a range was similarly obtained by other investigators using simulation [11, 61, 76]. This provided confidence in the simulation being conducted for the progressive forming process.

The horizontal velocity of the material under the punch has been evaluated via FE simulation to investigate the flow direction during microforming. Two distinct movement of material under the punch (one flowing outwards away from the area under the punch, and one flowing inwards towards the die orifice) could be observed. Consequently, the location of the neutral plane can be investigated and identified.

4.5.1.1. Analyzing the DMZ leakage using FE simulation

Figures 4-15-a to 4-15-h present the location of the neutral plane for different punch and die diameters just before punch displacement of 2.3 mm (Similar conditions compare to experiments). Two distinct material movement

directions in X and $-X$ direction (in Cartesian coordinate system which represents R direction in the cylindrical coordinate system) can be seen in these figures. Only the portion of material which moves toward $-X$ direction participates in pin forming.

Caption in the next page ...

Figure 4-15. The velocity vectors in x-direction (FE simulation result) during microforming after punch stroke of 2.3 mm for: 3.2 mm Punch with different pin diameters: (a) 0.8^W mm, (b) 0.8 mm, (c) 0.5 mm, (d) 0.3 mm; and 2.0 mm Punch with different pin diameters: (e) 0.8^W mm, (f) 0.8 mm, (g) 0.5 mm, (h) 0.3 mm. Shadowed zones in the picture represent DMZ in the microstructure extracted from the experimental observations.

The shadowed areas in these images superimposed onto the simulation results represent the DMZ obtained from the experiments, based on the

hardness measurements. By combining the location of the DMZ2 and neutral plane for each condition, the origin and occurrence of the DMZ2 at the pin surface can be visualized.

In the case of pins produced by the 3.2 mm punch (Fig. 4-15-a to Fig. 4-15-d), the neutral plane has crossed the DMZ2 in the head part. Thus, the material flowing in the negative radial direction ($-X$ direction) pushes the DMZ2 into the die orifice. Consequently, the micro-pin with the DMZ on the surface is obtained. It is the same for the 0.8^W pin produced by 2.0 mm punch (Fig. 4-15-e).

In the case of Fig. 4-15-b (3.2 mm punch and 0.8 mm pin), due to the severity of material flow in the head part, the collision of the DMZs under the punch happened, which caused an abnormally shaped DMZ2. Since the punch diameter is relatively large, the DMZ2 area is large and the neutral plane has crossed the DMZ2. Thus, the material flow pushed the material inside the DMZ2 towards the die orifice, even by using dies without entrance fillet radius. In this case, as shown in Fig. 4-15-b and Fig. 4-12-b the length of the DMZ on the pin surface was shorter than that of the 0.8 mm die with entrance fillet radius. In the case of dies without entrance fillet radius, the material in the DMZ2 could not penetrate into the die hole as much as the 0.8^W mm die (with fillet radius die).

For the 0.5 mm pins (Fig. 4-15-c and 4-15-g), the position of the neutral plane is nearer to the die orifice, compared to 0.8 mm pins (Fig. 4-15-b and 4-15-f). This indicates that there is more lateral movement of material (in X

direction). This is because of the relatively bigger punch to pin diameter ratio which makes the process similar to upsetting rather than extrusion. The same behavior was seen for the 0.3 mm pins (Fig. 4-15-d and Fig. 4-15-h).

Changing die diameter to the 0.3 mm, resulted in the microstructure shown in Fig. 4-12-d. Considering this figure together with Fig. 4-15-d, it can be seen that just beside the neutral plane there is a hump in the DMZ2. The material movement in X direction pushed the DMZ2 away from the die orifice, and caused this kind of hump in DMZ2 shape. In this case, there is a collapse of DMZ1 and DMZ2 at the very end of the stroke, due to the neutral plane location in the head, resulted in a squeeze of the softer material into the die orifice (at the bottom of the formed pin - see figure 4-12-d).

However, by using a smaller punch (2.0 mm punch in Fig. 4-15-h), there has been less DMZ development observed under the punch (in the head) which reduced the possibility of the neutral plane crossing the DMZ2. Thus, there is no DMZ2 leakage into the die orifice as the material-flow pushes the DMZ2 towards the outside away from the die entrance (in X direction).

In the case of Fig. 4-15-f, the neutral plane crossed the DMZ2 for the 0.8 mm pin with 2.0 mm punch. Therefore, it should be expected to see the DMZ on the pin surface, due to the material flow towards the die orifice. However, that was not the case. In fact, since the punch diameter is relatively small, the DMZ2 is smaller. So the material flow is not severe enough to push the DMZ2 inside the die orifice. Using analytical modeling, it could be possible to predict the critical punch to die ratio, for which the DMZ leakage does not occur.

From the comparison of either Fig. 4-15-a and Fig. 4-15-b or Fig. 4-15-e and Fig. 4-15-f, it showed that the die entrance fillet radius has a small effect on the neutral plane location under the punch. In the case of dies without entrance fillet radius, the neutral plane is diagonal. The die entrance fillet radius makes the material flow easier towards the die orifice, and that is the reasons why the neutral plane is slightly nearer to the die orifice in the case of dies with entrance fillet radius.

The assumed friction factor was found to affect the overall location of the neutral plane slightly. By increasing the friction factor, the neutral plane moves towards the outside of the punching area, indicating that more material will flow towards the die orifice.

Generally, the effects of process conditions on the existence of the DMZ in the pin microstructure can be summarized in the Table 4-2. As stated in this table, based on the experimental observations together with simulation analysis, there are three main geometrical parameters in this process affecting the DMZ formation on the pin surface: (i) entrance fillet radius, (ii) punch and (iii) die dimension. Usually the die diameter is fixed by the required application of the micro-pins. Thus, by changing the punch and entrance fillet radius, one could affect the material flow.

By using dies without an entrance fillet radius, the location of the neutral plane slightly moves towards the die orifice, and thus the leakage of DMZ2 to the die orifice can be further prevented. It is to be noted that machining of the

entrance fillet radius will become very difficult when the die diameter is decreased.

Table 4-2. Effect of process parameters on the DMZ size, neutral zone position, and the flow of material in the progressive microforming process.

Microstructural parameters Process parameters	DMZ size	Neutral zone position	Material flow
Increase in the punch diameter	Increase	Moves towards the die orifice	More material flow towards the die orifice
Increase in the die diameter	Almost no effect	Moves away from the die orifice	More material flow towards the die orifice
Increase in the entrance fillet radius	Almost no effect	Slightly moves towards the die orifice	More material flow towards the die orifice

4.6. FE Simulation validation; Friction model

To make sure about the reliability of the FE simulation results, the input boundary conditions and parameters must be elaborated. The shear ($\tau = mk$) and Coulomb ($\tau = \mu p$) friction models with constant coefficients were first implemented separately in the finite element model. Here, τ is the friction stress, m the shear friction factor, k the shear yielding strength of the material, μ the Coulomb friction coefficient, and p is the normal pressure on the interface.

As illustrated in Fig. 4-16, using the shear friction model, it was impossible

to get a well-matched situation. Indeed, the friction factor seemed to have changed during the process. The same scenario occurred in case of the coulomb model.

Figure 4-16. Simulation results of the Pin height vs. Punch stroke for different global friction factors. Pin diameter of 0.3 mm manufactured by 1.2 mm punch.

Therefore, a hybrid model which is a combination of both previous models was used. In this model, the shear friction model is used for locally high normal pressure interfaces, and the Coulomb friction model is used in locally sliding interfaces with less normal pressures. In other words, based on the local normal pressure on the interfaces, when $mk \leq \mu p$, the shear friction condition, and when $mk \geq \mu p$, the Coulomb friction condition will be considered by the simulation software.

The next step was to define the values of the m and μ for the finite element software. Based on von Mises criterion, it is known that for constant friction coefficient in the fully contact state, $\mu_{max}=0.577$ [111]. Values higher than this value in metal forming are meaningless [184].

On the other hand, the upper limit of the friction factor m is equal to 1. Therefore, in this study, the upper limit friction criteria considered for definition of the friction coefficients to the finite element software (Eq. 4-1):

$$\mu=0.577 \times m \quad (4-1)$$

Consequently, based on the value of m , the value of μ for the hybrid model was defined and used in the DEFORM-2D software. In this work, the value of m is only reported. After doing reverse engineering method to fit the experimental results, the value of the hybrid friction factor for different pin diameters had to be set between 0.55 and 0.65 in the simulation (see Fig. 4-17). The friction factor was increased marginally by decreasing the die diameter. In fact, the friction factor itself does not depend on the macro geometry. It depends on the interfacial properties. However, this apparent change in the friction factor is due to increase in the ratio of area-to-volume of the material flowing inside the die orifice, which could lead to increase in the interfacial normal pressure. In using this method, the mismatch error between simulation and experimental results was a maximum of 15% in all cases, which is attributable to the elastic deflection of the tooling.

It would be interesting to find out a universal solution for the optimum friction factor that must be used for the simulation of the process. This will be explained at the next sections.

As can be seen in Fig. 4-17, there was a critical punch stroke (strip thickness) after which there was a rapid increase of the pin height. Since the main pin forming occurs after this critical thickness, it can be used to optimize the process performance (Chapter 7).

Simultaneously, a good fit can be found in the case of forming load during the process for different process geometries (Fig. 4-18). As can be seen, the critical thickness (implemented from Fig. 4-17) is matched well with the result presented in Fig. 4-17, after which there is a significant increase in the forming load.

Figure 4-17. Pin height during the progressive microforming process for different die diameters. “m” in the curves represents the hybrid friction factor values used in the simulation.

Figure 4-18. Evolution of the required forming load during the progressive microforming process for different die diameters. “m” in the curves represents the hybrid friction coefficient values used in the simulation.

4.6.1. Grid-line Distortion; Material Flow

To make sure about the validity of the FE simulation results, the grid-line method was utilized. As can be seen in Fig. 4-19, the overall grid-lines’ deformation figures were matched qualitatively between simulation and experiment. Three deformed elements in the simulation grid-line profile were selected as well as similar three elements in the half cross-section of the real pin in the mirror position (Fig. 4-19). The measurements of the deformation angles were done using the ImageTool software version 3.

Caption in the next page ...

Figure 4-19. Grid-line distortion and shear strain validation in simulation and experiments in the 0.8 mm pin manufactured by 3.2 mm punch in different punch strokes of: (a) 1.5, (b) 2, and (c) 2.3 mm.

As can be seen in Fig. 4-19, there is a good quantitative match between the shear strain values in the simulated grid-line profile and the experimental results. Therefore, the simulation results are reliable, using the mentioned boundary conditions. The next step is to predict the friction factor using simulation for other geometrical conditions.

4.6.2. Optimum friction factor for different process geometries

To generalize the results, the behavior of the hybrid friction factor was studied quantitatively. This was done using the response surface method (RSM) embedded in Design-Expert[®] software version 8. The RSM was done at the only one-factor level. To do so, the most suitable hybrid friction factor for different punch/pin diameter was predicted, using the method stated in previous sections, presented in Table 4-3.

To quantify the choice of hybrid friction factor, Analysis of Variance (ANOVA method) with regression analysis was used. The output from the ANOVA analysis using the software Design Expert[™] was collated in Table 4-4. In this table, the pin and punch diameters were considered as A and B factors, and based on the variance analysis, the regression model as Eq. 4-2 was predicted. The Model with the F-value of 31.57 has only 0.01% chance to occur; the noise indicated that the model is significant. Qualification of the

influence is indicated by the p-value ("Prob > F"), where a value less than 0.0500 indicates model terms are significant, whilst values greater than 0.1000 denote that the terms are not significant, excluding those terms required to support model hierarchy.

Table 4-3. Hybrid friction factor, estimated by fitting the simulation results to experimental. The experiment with 5.0 mm punch and 0.3 mm pin was not possible due to our servo-press capability.

Pin (mm) \ Punch (mm)	Punch (mm)	1.2	2.0	3.2	5.0
0.3		0.65	0.675	0.7	-
0.5		0.475	0.6	0.675	0.7
0.8		0.45	0.475	0.55	0.6
1.2		-	0.475	0.525	0.55

The resulting regression model was reported as Eq. 4-2 for dry condition.

$$m_{Hybrid} = 0.78101 - 0.74499d_{Pin} + 0.33970d_{Pin}^2 + 0.041212d_{Punch} \quad (4-2)$$

Standard statistical measure for qualification of the regression model is found in Table 4-5. The "Pred R-Squared" of 0.8335 was in reasonable agreement with the "Adj R-Squared" of 0.8759, indicating that there are neither blocks (outliers) nor need for transformation of the response values. The "Adeq Precision" measures the signal to noise ratio. As the ratio was 15.666, which is greater than the desired value of 4, the model can be used for prediction by interpolation of the range of pin and punch diameters investigated. A graph of

the friction factor is shown in Fig. 4-20.

Table 4-4. ANOVA analysis for Response Surface Reduced Quadratic Model.

Analysis of variance table [Partial sum of squares - Type III]						
Source	Sum of Squares	df	Mean Square	F Value	p-value Prob > F	Effect
Model	0.097	3	0.032	31.57	< 0.0001	Significant
A(Pin diameter)	1.461E-003	1	1.461E-003	1.43	0.2601	Hierarchical argument
B(Punch diameter)	0.040	1	0.040	38.72	< 0.0001	Significant
A ²	0.012	1	0.012	11.90	0.0062	Significant
Residual	0.010	10	1.025E-003			
Cor Total	0.11	13				

Table 4-5. Statistical measure for the regression model.

R-Squared	0.9045
Adj R-Squared	0.8759
Pred R-Squared	0.8335
Adeq Precision	15.666

Figure 4-20. Influence of the geometry on the hybrid friction factor.

Increasing the punch diameter leads to an increase in the hybrid friction factor. The influence of the pin diameter is opposite, where a decrease of the pin diameter leads to an increased hybrid friction factor. Both these geometrical effects may cause an increase of the pressure at the contact surface changing the nature of the contact between the workpiece and the tool surface. This implies that the nature of friction has pressure dependence, which is difficult to verify experimentally. Simulation results, however, showed that either by increasing the punch diameter or decreasing the pin diameter, the normal pressure at the interface increases.

4.7. Friction effects

4.7.1. Phenomenological Studies

As reported, in the open-die forging/extrusion processes, there is always a neutral plane which separates the material flow directions; one towards the outside of punching area and one towards the die orifice. The location of this neutral plane is very important as it determines the final part geometry and mechanical properties.

As a matter of fact, there are mainly two phenomena involving in the progressive microforming process: (i) Upsetting, and (ii) Extrusion. The neutral plane appears as the extrusion phenomenon begins. Before that, there is mainly upsetting occurs during the process, and material mainly flows outward of the punching area rather than flowing towards the die orifice. Therefore, the location of the neutral plane determines the dominant phenomenon occurring in the process. This concept can be used for controlling the material flow in the process.

To have a better understanding about the physics of the process, based on the phenomenological study, the effect of friction was separately investigated in two different zones of I and II, as shown in Fig. 4-21.

In one set of simulation runs, various friction factors were set on the punch surface and die top-surface interfaces (shown by m_I as the friction factor in Fig. 4-21), while keeping the friction factor constant inside the die orifice. Doing so,

the effect of friction mainly in upsetting phenomenon would be studied; although it involves a bit of the extrusion phenomenon.

Figure 4-21. Schematic half-cross-section of the forming process showing the material flow by velocity vectors via simulation. Neutral Plane separates the material flow in two opposite directions under the punch.

In the other run, the friction factor on the die top-surface and the punch interface (m_1) was kept constant, and the friction factor inside the die orifice (m_2) was set at different values. This way, the friction effect only in the extrusion phenomenon was investigated. Thus, the effect of lubrication on the die top surface can be compared to that of inside the die orifice.

The simulation results here were shown only for the pin diameter of 0.8 mm manufactured by the 3.2 mm punch. The results for other pin diameters mentioned in this report followed the same trend.

4.7.2. Friction effects on the behavior of neutral plane

The effect of individual friction factors (m_1 and m_2) on the location of the neutral plane is plotted in Fig 4-22. Interestingly, by increasing m_1 , the radius of the neutral plane increases (Fig. 4-22-a). This shows the movement of the neutral plane outwards (towards +X direction in Fig. 4-21) by increasing m_1 . Indeed, since the main phenomenon occurring on the die top-surface is upsetting, therefore as previously mentioned by Avitzur [155], increasing the friction factor increases the neutral plane radius to reduce the forming energy.

This behavior is opposite to the case where m_1 is constant and there is an increase in m_2 (Fig. 4-22-b). Increasing m_2 leads to increase in the resistance of the metal to flow inside the die orifice. Thus, by punching, the metal tends to be flowing outwards of the punching area rather than flowing towards the die orifice. Consequently, the neutral plane moves towards -X direction.

It is important to state that not all the friction factors presented in Fig. 4-22 are necessarily achievable physically. This curve provides a theoretical insight into the material behavior during the process.

Figure 4-22. The effect of friction on the location of the neutral plane during the process for different process geometries. (a) in the zone I for various values of m_1 , (b) in the zone II for various values of m_2 .

4.7.3. Friction effects on the final part dimension

As stated, the location of the neutral plane determines the amount of material flowing towards the die orifice and participates in the actual pin forming. To

see the effect, the pin aspect ratio (pin height over pin diameter) was monitored during the process for different friction conditions (Fig. 4-23).

Figure 4-23. The effect of friction on the pin aspect ratio during the process for different process geometries. (a) in the zone I for various values of m_1 , (b) in the zone II for various values of m_2 .

As plotted in Fig. 4-23, the same trend as Fig. 4-22 can be seen in case of the pin aspect ratio; although, the increase in the pin aspect ratio by increasing m_1 is not very significant (magnified in Fig. 4-23-a). In fact, since m_1 mainly affects the upsetting phenomenon in the process, any increment in the friction factor would push the neutral plane outwards, which in return pushes the material flowing towards the die orifice. But it also affects a bit of extrusion on the top part; which makes it a bit more difficult for the material to flow towards the die orifice and to form the pin. As a result, these two opposite effects counteract each other and thus no significant increase in the aspect ratio curve can be observed.

The effect of m_2 is significant though. Changing the friction conditions inside the die orifice only affects the extrusion phenomenon in the process. Therefore, by increasing m_2 , there is more resistance for the material to flow inside the die orifice. As a result, increasing m_2 leads to relatively significant decrease in the final pin aspect ratio achievable by the process (Fig. 4-22-b).

Consequently, the optimum friction condition in the process (to get the largest pin height) can be defined as the maximum friction factor on the die top surface and punch surface (m_1), and the least friction factor on the wall of the die orifice (m_2). However, it can affect the required forming pressure. The overall effects are summarized in Table 4-6.

Table 4-6. Effect of increasing the localized friction factor on the process parameters.

	Neutral Plane Radius	Pin Aspect Ratio	Forming (Punch) Pressure
m_1	Increase	Slight Increase	Increase
m_2	Decrease	Decrease	Slight Increase

4.7.4. Friction effects on the required forming pressure

As shown in Fig. 4-24-a, increasing m_1 significantly affects the punch pressure required for pin forming. The effect for the m_2 is less obvious. The reason is simple as there is more surface involved on the top-surface of the die set (m_1) rather than inside the die orifice (m_2).

Figure 4-24. The effect of friction on the required forming pressure during the process for different process geometries. (a) in the zone I for various values of m_1 , (b) in the zone II for various values of m_2 .

The result in Fig. 4-24-b is consistent with the work reported by Chan *et al.* [25]. They mentioned that in extrusion process, the majority of forming energy (or forming pressure) is consumed by plastic

deformation, and frictional effects play a minor role in energy consumption.

4.8. Summary

It is possible to manufacture micro-pins without any defect using strips in the progressive microforming process, but there can be inhomogeneous mechanical properties (defined as dead metal zone) in the micro-pins formed depending on the geometrical parameters chosen. From the microstructure study, it was identified that there are regions in the head, under the punch and at the die orifice entrance, which were less deformed as compared to other parts. These so-called DMZs were the main cause of the inhomogeneous mechanical properties in the pin. Thus, one needs to choose the proper punch diameter and using dies without entrance radius, so that the soft zone at the pin surface can be eliminated, and consequently, the mechanical properties of the micro-pin can be more homogeneous.

There are two types of material flow in the head of the micro-pin; one towards the die orifice, and one directed outwards away from the die orifice. This leads to the formation of a “mobile” neutral plane under the punch which separates the two material flows. The location of this neutral zone was investigated by FE simulation. Coupling FE simulation results with experimental results showed that the crossing of the neutral zone and dead zone under the punch can be used as one indicator on whether the existence of the soft zone at the pin surface will occur. Therefore, by using simulation to

identify the location of the neutral zone and changing the process condition accordingly, one can create a micro-pin with more homogeneous microstructure produced by the progressive forming process or otherwise.

The progressive microforming process is a type of open-die forging/extrusion process, which involves two main phenomena; (i) Upsetting and (ii) Extrusion. Changing the frictional conditions for each of the phenomena involved in the process has different effects on the material performance as discussed in this chapter. To get the highest pin, it is suggested to have the highest friction factor at the die surface and the lowest friction factor inside the die orifice.

Calculation of the strain distribution in the cross-section of the feature in the forming process, using grid-line method, can be used as a powerful quantitative tool for modeling verification. Results showed that using the Hybrid friction model in the finite element modeling gives the best fit in the process.

By decreasing the die diameter, the friction factor was increased. This is related to the size effect in the process as by decreasing the die diameter, the fraction of area to the volume of the material inside the die orifice increases. This fact was quantified for different geometrical conditions in the process using ANOVA analysis, and a model is suggested to predict the friction factor based on respective pin and punch dimensions. From our study, it was also shown that the friction factor is not a constant value, but depends on the punch and die dimensions.

Increasing the friction factor on the die top-surface and punch surface (m_1), together with decrease in the friction factor on the die wall inside the orifice (m_2) is the best frictional condition to get the highest pin aspect ratio in the open-die forging/extrusion process.

Knowing the phenomenon occurring during the process, it would be interesting to explore the effect of grain size on the material flow. This will be the topic of the next chapter.

Chapter 5: Grain Size and Workpiece

Dimension Effects

5.1. Introduction

The mechanical behavior of a polycrystalline metal mainly depends on its microstructure and the grain size [11]. As modeled by the Hall-Petch relationship (Eq. 5-1), for grains with a diameter of larger than a few microns, a decrease in grain size leads to a general strengthening (Grain Size Effect) [39].

$$\sigma = \sigma_0 + \frac{k}{\sqrt{d}} \quad (5-1)$$

where σ_0 represents the grain interior resistant to deformation, and k is the strengthening coefficient. Both these parameters are constant at a given strain, based on Hall-Petch explanation [185, 186].

Nevertheless, it has been reported that the thickness of the workpiece must be considered together with the grain size to determine the mechanical response of the material during a microforming process [75]. In fact, mainly based on the stacking fault energy of the metal,

there is a critical number of grains over the thickness of the workpiece, below which the mechanical behavior of metal does not follow the Hall-Petch relationship (Grain-Specimen Size Effect) [21, 57, 70-74]. This is the so-called size effect that causes unexpected material behavior during microforming processes.

In fact, when the grains' dimensions become large in comparison with dimensions of process, two phenomena may happen: (i) deformation becomes harder due to few grains to accommodate the incompatibility between grains; (ii) the scatter in results becomes noticeable giving variations from test to test.

There has been reports discussing about modification of the Hall-Petch equation with regards to the effects of grain size and specimen dimensions [82-86]. Despite the huge number of the available reports regarding the grain-specimen size effects on the material's mechanical response during the microforming processes, there are very limited publications about the grain-specimen size effect on material flow or microstructural behavior during these processes.

5.1.1. Scope of this chapter

The effects of process-related parameters on the material flow during the process were discussed in the previous chapter with much focus on the dead metal zone (DMZ) behavior.

In real applications, the material-related parameters have a close interaction with the process-related parameters. In this chapter, the interactive effect of initial grain size and specimen dimensions on the material flow and microstructural evolution during the process is evaluated. Particular interest is paid to the effect of the different number of grains over the specimen thickness on the evolution of the DMZ in the micro-pins' microstructure. The effect on the dimensions of the pins and the forming force were also monitored to have a comparison with previous size-effect studies. To consider the effect of the number of grains over the cross section (grain-specimen size effect), specimens of different grain sizes and two different thicknesses were used.

5.2. Initial material properties

Figure 5-1 shows the microstructure of the strips annealed at different temperatures for 1 hr. As can be seen, there is a good range of grain sizes (from about 14 μm to around 110 μm) for investigation. Annealing at longer durations had no significant effects on the grain size.

Figure 5-1. Optical micrograph, mean grain size together with the grain size distribution of the C11000 copper alloy annealed at different temperatures of: (a) as-received; (b) 400°C; (c) 600°C; (d) 800°C; for 1 hr.

To normalize the results, the number of grains over the thickness of the strips was calculated as presented in Table 5-1. The 2.5 mm strips were used to study the grain size effect, while the 0.5 mm strip with coarse grains was used to investigate the grain-specimen size effects.

Table 5-1. Mean number of grains over the thickness for different strip thicknesses annealed at different temperatures for 1 hr.

Strip thickness (mm)	Annealing temp.(°C)			
	As-received	400	600	800
2.5 (thick)	180	45	26	23
0.5 (thin)	36	9	5.2	4.5

Figure 5-2 illustrates the effects of the annealing temperature on the mechanical properties of the C11000 copper alloy. From the slope of the flow curves of the annealed specimens, it can be concluded that annealing above 400°C has a negligible effect on the strain hardening of the alloy. Therefore, by annealing at the before-mentioned temperatures, it is ensured that there is only the grain size being altered, without significant changes in the general mechanical behavior of the metal.

Figure 5-2. Room temperature average flow stress curves of the CC11000 copper alloy achieved by upsetting test after annealing at different temperatures for 1 hr.

5.3. Grain size effect (for the 2.5 mm thick strip)

The pin height and the maximum forming load were recorded. As can be seen in Fig. 5-3, there is a slight increase in the average pin height from 2.5 to 2.6 mm by increasing the initial grain size. This marginal increase can be attributable to the annealing treatment that softened the material leading to a slight ease of material flow into the die orifice in the initial stages of the process. However, it is difficult to make a robust conclusion from this figure as the results overlap on their standard deviation.

Figure 5-3. The effect of grain size (annealing temperature in 1 hr) on the final part dimensions in the open-die micro-forming process. (For the pin diameter of 0.3 mm manufactured from the 2.5 mm strip thickness).

As mentioned in previous chapter, the micro-part dimension in this type of open-die process was determined by the geometrical conditions. In other words, the geometry of the process together with the frictional conditions govern the location of the neutral plane. The location of the neutral plane determines the amount of material flowing towards the die orifice, which forms the final micro-part. Therefore, the initial properties of the forming metal theoretically have no effect on the final part dimensions under these conditions.

The required forming load during the process for specimens of different grain size is shown in Fig. 5-4. As explained in more details in the Chapter 4, the load-stroke curve consisted of three stages, including indentation at the beginning, the middle stage of upsetting with lower slope, and the final

extrusion stage with a relatively sharper slope. As can be seen in this figure, increasing the grain size leads to lowering the forming load during the first two stages. Indeed, at the beginning of the process, the material only goes through upsetting. In upsetting, by increasing the grain size, there is less force required for metal flow. However, by reaching the final stage, where the metal mainly flows towards the die orifice (extrusion), there is no significant difference in forming load for different specimens. This is due to the fact that by a relative increase of the grain size compared to the die diameter, it would be more difficult for the metal to flow inside the die orifice, as reported by Wang *et al.* [130] in a micro-coining process. Therefore, by increasing the grain size, although the metal gets softer, more redundant work must be done to develop a network of dislocation cells inside the grains and break up the grains to smaller ones to flow inside the die orifice. The balance between softening caused by coarse grains and the redundant work makes the total force relatively closer to the as-received one at this stage. This again implies no significant grain size effect on the peak force.

The grain size effect is, however, significant in the microstructural observations. Figure 5-5 represents the cross-sectional microstructure and hardness profile of the 0.3 mm pins formed from the strips with various grain sizes. As explained in a previous publication [187], there is a DMZ leakage to the die orifice for the 0.3 mm pin formed by the 3.2 mm punch from the as-received metal.

Interestingly, the volume of the DMZ in the pin microstructure was decreased by increasing the initial grain size. The main reason for this behavior can be attributed to the strain gradient through the microstructure associated with the formation of dislocation cells inside the grains [89, 94, 188, 189].

Figure 5-4. Load-Stroke curve for pin forming using strips annealed at different temperatures for 1 hr (different grain sizes) (For the pin diameter of 0.3 mm manufactured from the 2.5 mm strip thickness).

Indeed, a larger DMZ is related to more inhomogeneous metal flow [190]. The inhomogeneity in the metal flow is identified via investigating the strain gradient distribution in the microstructure [93, 191]. It has been found that the amount of the effective-strain-distribution is directly related to the dislocation cell size and/or subgrain size [192]. In fact, smaller cell size corresponds to higher strain/strain distribution in the microstructure. Decreasing the dislocation cell size (or subgrain size [193]) leads to decreasing the dislocation free path which in return affects the effective strain value inside the grains. On the other hand, the dislocation cell size depends on the initial grain size by square root

relationship: the smaller the grain size, the smaller the cell for a given strain [59].

Caption in the next page ...

Figure 5-5. Microstructure and microhardness profile (Hv) of the 0.3 pin manufactured from the 2.5 mm strips which were annealed at different temperatures of: (a) as-received; (b) 400°C; (c) 600°C; (d) 800°C, for 1 hr.

Figure 5-6 presents the inverse pole figure EBSD maps of the head-part of the 0.3 mm pin manufactured from different strips with various initial grain sizes. Un-deformed grains on the top (Region 1), followed by elongated grains at the middle (Region 2) with an elongated shape relative to the flow direction, and equi-axed smaller grains at the bottom (Region 3) corresponding to the DMZ. In fact, the grains at the bottom (inside the DMZ) are stationary and only flatten under compression. With increasing deformation, due to extrusion of subgrains, grain boundaries serrate progressively. Once the thickness of the original grain decreases to about the size of two subgrains, the grain boundaries come into contact, and hence the grains split up [194]. Having that mentioned, any delay in formation of the subgrains from dislocation cells with critical size, could lead to delay in development of DMZ. As reported by Guzel *et al.* [194], this fact can be monitored by measuring the subgrain size through the microstructure.

The substructure of a random location at each region for each specimen has been plotted at the right of each figure. It can be qualitatively seen that by moving from Region 1 to Region 3, the subgrain size decreases for each specimen. This fact is shown quantitatively in Fig. 5-7.

Caption in the next page ...

Caption in the next page ...

Figure 5-6. (a) Schematic of the half-cross section of the micro-pin showing the location of the inverse pole figure (IPF) EBSD maps taken from 0.3 mm pins, manufactured from different strips under initial conditions of: (b) As-received; annealed at: (c) 400°C; (d) 600°C; (e) 800°C. Regions assigned as 1, 2 and 3 represent the regions with different grain structures. Length of these regions has been schematically shown in Fig. 5-6-e. The substructure of a random bunch of grains in each region has been shown in the magnified images at right, showing grain boundaries with different misorientation angles.

Data illustrated in Fig. 5-7 has been plotted for at least 3 measurements. As illustrated in this figure, for each sample the subgrain size decreases by moving from the top part of the head towards the bottom. This is related to the strain gradient that exists within the microstructure in the head part.

Figure 5-7. (a) Mean grain size, and (b) Mean subgrain size of the 3 regions shown in Fig. 5-6, for the 0.3 mm pin manufactured from the strips annealed at different temperatures.

In another view, by increasing the grain size, the size of dislocation cells and subsequent subgrain size became bigger. Dislocation theories determine lower

stresses being applied on the bigger dislocation cells [195]. The overall applied stress (which is the same for all samples) in coarse-grained samples consumes for generation of new dislocations and development of dislocation structures. However, in fine-grained metals, cell size and subsequently, the dislocation free path is smaller, and therefore, higher fraction of the overall applied stresses concentrates on the subgrain boundaries by dislocation pile-ups. This induces a plastic strain gradient inside the microstructure of the fine-grained metals. Chan and Fu [59] reported a similar behavior, and they mentioned that this phenomenon becomes clearer by increasing the number of grains over the cross-section of the specimen. This fact will be investigated in future sections.

The other fact that is extracted from Fig. 5-6 is the relative length of the three regions for each specimen. As shown in Fig. 5-8, increasing the initial grain size of the metal strip leads to decrease in the fractional length of the Region 3. This literally proves the data shown in Fig. 5-5 (increasing the volume of DMZ by increasing the initial grain size of strips before forming).

Figure 5-8. Fractional length of the three regions throughout the cross section of the head part of the 0.3 mm pin manufactured from strips having different grain sizes.

To summarize, in metals with larger grain size, there is a delay in development of DMZ due to larger dislocation cell size in the microstructure. Thus, the DMZ volume will be smaller and subsequently there will be no leakage of the DMZ inside the die orifice.

It is to be noted that by looking in detail of the microstructure in the Fig. 5-5-d, there is a very narrow DMZ leakage to the die orifice. However, the volume of this DMZ was very small and could not be captured by the micro-indentation test. Therefore, it is not obvious in the hardness profile.

To have a better understanding about the development of such a DMZ within the structure, the microstructure of the micro-pin manufactured from the coarse-

grained specimen (annealed at 800°C), were investigated during the process in different punch displacements of 1, 1.5, 2 mm (Fig. 5-9).

As can be seen, the DMZ first develops under the punch, at the top surface of the head, due to interfacial friction forces. Interestingly, at the beginning, there is no DMZ on the die surface (bottom part of the head), and it develops only at the end of the process. This trend is a bit different from what has been observed for the as-received specimen with smaller grain size.

In this case, using a coarse-grained metal as the initial strip, the dislocation cells were not developed thoroughly within the microstructure. In other words, the large grains filled a considerable volume of the head part, and so the whole grain structure will experience deformation leading to less separation of the deformed and less/non-deformed region. Consequently, there would be less inhomogeneous deformation, and therefore, there were smaller less-deformed zones (or DMZs) in the head part.

Figure 5-9. Development of the DMZ in the “head” part of the micro-pins’ structure through the process in different punch displacement of: (a) 1 mm; (b) 1.5 mm; (c) 2 mm (for the 0.3 mm pin manufactured from the coarse-grained strip). Hardness values in Hv. Gray zones show the DMZ (softer regions)

5.4. Grain-Specimen size effect (for the 0.5 mm thick strip)

As mentioned by Keller [75], the size effect would be only obvious if the number of grains over the cross-section of the metal goes below the critical number, which is around 8 for copper. Based on Table 5-1, using the annealed strip at 800°C with thickness of 0.5 mm, would provide such a condition. Figure 5-10 compares the dimension of the 0.3 mm pins manufactured from the thin and thick strips (0.5 mm thickness) with different grain size. As can be seen, there is a negligible grain size effects on pin height, which again supports the hypothesis of the key role of the neutral plane on determination of the part dimension in open-die forming processes.

Moreover, in Fig. 5-10 comparing the pin height achieved at two different strip thicknesses for each annealing condition, shows that the main portion of pin forming (about 70%) occurred at the very end stroke of the process (the last 0.5 mm thickness). This implies that there must be a critical strip thickness, after which the metal flows mainly towards the die orifice, and the pin forms mainly afterwards. This fact will be elaborated in the subsequent chapters.

Figure 5-10. The effect of grain size on the final part dimensions in the open-die micro-forming process. (For the pin diameter of 0.3 mm manufactured from strips with initial thicknesses of 0.5 and 2.5 mm)

Comparing the forming load in the pin forming of the thin and thick strips it was found that the amount of the upsetting phenomenon was decreased for the pins manufactured from thin strips (Fig. 5-11). This is so because decreasing the strip thickness results in the earlier development of the neutral plane under the punch [187]. As a result, the metal would mostly flow towards the die orifice at the early stages. In other words, the share of the upsetting phenomenon in the process is relatively less for the thin strips.

Moreover, it was seen that the maximum forming load is almost similar to the required forming load of the thicker strip, with a very slight decrease. This is due to the reduced amount of the upsetting phenomenon on the overall process.

Figure 5-11. Load-Stroke curve for 0.3 mm pin forming using samples having different grain sizes with: a) thick strip (2.5 mm thick); and b) thin strip (0.5 mm thick).

From the microstructures obtained (Fig. 5-12), it was observed again that there was no DMZ formed at the pin surface for the case of the thinner strips. In comparison, as can be seen in Fig. 5-5-d, there is a small region at the bottom of

the pin, in which the grains are highly deformed and a bit elongated along the deformation route. This region was not seen for the case of the coarse-grained pin manufactured from the thin strip (Fig. 5-12). For this case, Keller *et al.* [75] mentioned that as the number of grains over the thickness of the workpiece decreases to below its critical value, there is an insufficient number of grains to develop deformation (shear) bands in the microstructure. As a result, for specimens having just a few grains over the cross-section (below the critical value), it is unlikely to see very obvious shear bands being developed in the grains' structure. Therefore, the grains on the pin microstructure (Fig. 5-12) appear like equi-axed grains without any significant deformation (shear) occurred in them through the process.

Figure 5-12. Microstructure of the 0.3 mm pin manufactured from the strip with the thickness of 0.5 mm, which was annealed at 800°C for 1 hr.

5.5. Summary

The grain size effect together with the thickness size effect in an open-die micro-forging/extrusion was experimentally analyzed. The microstructural size effect was also studied.

Open-die forging/extrusion processes can be introduced as processes which are not very sensitive to grain size effect, with respect to the mechanical response of the metal and also the final part dimensions. Indeed, in these processes, the location of the neutral plane and its development throughout the specimens play the key role.

Decreasing the number of grains over the thickness of the specimen has no direct effect on the material behavior and only affects the microstructural evolution during the process. In coarse-grained specimens, there is a delay in development of DMZ due to a larger dislocation cell size in the microstructure. Thus, increasing the grain size can be used as another alternative solution for preventing the DMZ leakage to the pin's microstructure.

After investigating the pre-production and in-production material behavior, in the next chapter the post-production properties of the micro-parts will be discussed focusing on the size effect. The indirect size effect in the process will be investigated via studying the subgrain-thickness interactions.

Chapter 6: Post-production Size Effects

6.1. Introduction

In all researches related to the size effect, the material was fully annealed before forming. It implies that the initial dislocation structure of all the previous studies was the same, with almost no initial dislocation cells or subgrain developed in the microstructure before forming. To the best knowledge of the author, there has not been any specific report out in the literature investigating the effect of initial subgrain structure, or post-production properties effects, on the mechanical behavior of miniaturized specimens.

In the last chapters, the microstructure of the micro-pins manufactured from strips with various initial grain sizes was investigated. It would be interesting to study the mechanical properties of these micro-pins having different grain sizes. However, in the as deformed state, all the micro-pins have a network of dislocation cells (or subgrains) within their microstructures. To have a comparison, one

batch of the micro-pins was recovery annealed (at 230 °C for 30 mins) to remove the dislocation substructure. The recovery annealing temperature is below the recrystallization temperature of the copper alloy and prevents any significant changes in the grain size.

6.1.1. Scope of this chapter

Therefore, the aim of this chapter is to understand the effects of post-production properties of the miniaturized specimens (feature size effects) on the mechanical behavior. Micro-pins with similar diameter but various grain sizes and different initial dislocation states were produced for micro-compression test. The EBSD technique was used for studying the substructure.

6.2. Post-production grain size effect

6.2.1. Hall-Petch breakdown

Two batches of micro-pins with a similar diameter of 0.3 mm were manufactured using strips having different initial grain sizes (As-received, and annealed at 400 and 800 °C as discussed in the previous chapter). One batch of the micro-pins was recovery-annealed (RA) to remove the dislocation substructure without changing the grain size. The punch diameter for production of the micro-pins was chosen as 2.0 mm to prevent the formation of the DMZ at the pin surface (refer to Chapter 4).

Micro-compression test showed no significant specimen-grain size effects in the mechanical response of the as-formed micro-pins (Without RA), since the standard deviations overlapped (Fig. 6-1-a). There is a small difference between the flow stress of the as-received and 800°C results, which is attributed to the Hall-Petch equation. Such a result indicates no size effects with regards to the mechanical properties of the pins, under these circumstances.

Figure 6-1. The effect of post-production recovery annealing (RA) on the average flow stress curves of 0.3 the mm micro-pins manufactured from the CC11000 copper alloy strips annealed at different temperatures.

Mechanical response of the RA micro-pins, however, showed a different behavior as plotted in Fig. 6-1-b. Compared to Fig. 6-1-a a decline in the flow stress for each pin can be noticed. The reason behind this behavior is related to the difference in dislocation annihilation mechanisms in RA and as-deformed samples, which will be elaborated in sections ahead.

It is worth noting that since the heights of the compressing micro-pins were relatively small (around 0.75 mm), there were only a few numbers of data points obtained from the micro-compression test, as plotted in Fig. 6-1.

The consistency of the results with the Hall-Petch equation for the as-formed micro-pins is shown in Fig. 6-2-a, with a linear relationship (with R^2 value of nearly equal to unity) between the flow stress and the inverse square root of the grain size ($\frac{1}{\sqrt{d}}$). In comparison, for the RA micro-pins, since the number of subgrains across the diameter is below the critical value, size effect is more significant on the mechanical behavior (Fig. 6-2-b). This is shown quantitatively with the R^2 values, which is relatively lower than that of Fig. 6-2-a.

Using Fig. 6-2, the change of σ_0 and k in the Hall-Petch relationship (Eq. 5-1) with strain is illustrated in Fig. 6-3. The gradients of both parameters are almost the same for the whole strain range.

As shown in Fig. 6-3-a, the σ_0 is smaller for the RA micro-pins for the whole range of strain, due to recovery annealing. The density of dislocation forests was decreased, and therefore, there was less stress required for dislocations' mobility inside the grains. This behavior also reveals that there is a lower dislocation density required in the grain interior of the RA micro-pins to accomplish deformation, as reported by Chan and Fu for coarse-grain specimens [59]. The increase of σ_0 with strain is related to the increase in the statistically dislocation density.

Figure 6-2. Flow stress vs. grain size for different strain obtained from the micro-compression test of micro-pins with different grain sizes, under different post-production conditions of: (a) as-formed, without recovery annealing; (b) after recovery annealing.

Figure 6-3. Change of: (a) σ_0 ; and (b) k ; with strain for the RA and the as-formed micro-pins.

The strengthening coefficient of k , in general, explains the significance of the role of the grain size on the overall mechanical properties. Figure 6-3-b

illustrates a relative increase in the k value with recovery annealing, for the whole strain range. As can be seen, recovery annealing on the highly deformed samples could significantly increase the k value for the whole strain range. This indicates that removing the dislocation structure by recovery annealing increases the sensitivity of the mechanical properties on the grain size. In other words, in recovery annealed specimens, by increasing the grain size, there will be relatively more changes on the mechanical properties, compared to the as-deformed specimens (See Fig. 6-1).

Figures 6-2 and 6-3 show that in addition to the specimen and grain size interactive effects, the substructure relative dimension must be considered in determining the size effects.

6.2.2. Microstructural studies

To justify the abovementioned behavior, the substructure of both types of micro-pins was investigated using EBSD mapping. Due to the symmetry of the micro-pin, only half cross-section of the pins was investigated using EBSD technique, as shown in Fig. 6-4. To reduce the EBSD timing, only a portion of the pins' microstructure was selected.

Figure 6-4. Schematic of the location of the EBSD mapping from half-cross section of the micro-pins.

In Fig. 6-5 high angle grain boundaries (HAGB, misorientation $>15^\circ$) are shown by black lines. It is worth noting that although most of the researchers consider the upper limit of 15° as the low angle grain boundaries (LAGBs), some use the 10° as a threshold misorientation [170]. So, to be in a safe zone, misorientations between 2° to 10° were considered as LAGBs. Misorientations between 10° to 15° are also shown for references. It can be concluded that considering the LAGBs misorientations as higher than 15° instead of 10° has no significant effects on the subgrain size reported here. The volume fraction of the twin boundaries in the microstructure is the same for all the micro-pins.

At the first glance, it can be qualitatively concluded that RA had no significant effects on the grain size (e.g. compare Fig. 6-5-a and Fig. 6-5-b). Although there was a good control over the recovery annealing, there was a

little recrystallization obvious in the microstructure, which is not avoidable. However, this has no significant effect on the mean grain size and can be ignored. In contrast, a decrease in the density of the LAGBs was observable after RA (e.g. the amount of silver-colored lines in Fig. 6-5-a vs. Fig. 6-5-b).

These findings were quantitatively obtained from the EBSD data as presented in Fig. 6-6. Clearly, the effects of pre-production annealing and post-production annealing and their interactive effects on the grain and subgrain sizes can be seen in this figure.

A general increase on the mean grain size by increasing the pre-production annealing temperature on the initial strips is observed for all the samples, regardless of the post-production (recovery) annealing (Fig. 6-6-a).

A different behavior is, however, observed for the mean subgrain size (Fig. 6-6-b). The subgrain size in the as-deformed micro-pins shows a very slight increase by increasing the initial grain size. This is because size of the subgrains developed during forming is directly related to the pre-forming (initial) grain size [83, 195]. The same trend is seen for the RA micro-pins, with more significant magnitude. Indeed, the recovery annealing coarsened the substructure size up to a size comparable to the mean grain size. It occurs via dislocation annihilation, and subgrain coalescence and growth [164, 196].

Caption in the next page ...

Caption in the next page ...

Caption in the next page ...

Figure 6-5. Inverse Pole Figure (IPF) EBSD maps obtained from half cross section of the 0.3 mm micro-pins manufactured from strips with various grain sizes (pre-processing annealing conditions), in different post-production conditions: (a) as-received initial strip, without post-production annealing; (b) as-received initial strip, with post-production RA; (c) annealed initial strip (at 400°C), without post-production annealing; (d) annealed initial strip (at 400°C), with post-production RA; (e) annealed initial strip (at 800°C), without post-production annealing; (f) annealed initial strip (at 800°C), with post-production RA. The grain boundaries with different misorientation angles are shown in a random area of each sample in the upper right corner of each figure.

It is worth noting that the RA transforms the subcells to the subgrains with larger misorientations. This reduces the hardening contribution of the dislocation forests; however, as shown in Fig 6-1, especially for the coarse-grained samples, the subgrain size gives a stronger Hall-Petch effect.

As mentioned by Keller [75], the size effect would be only obvious if the number of grains over the cross-section of the metal goes below the critical number, which is around 6 for copper. To have a comparison, the extracted data from Fig. 6-6 is presented in Table 6-1.

As can be seen, the number of grains over the diameter of the micro-pin (N_G = Pin diameter over grain size) decreases down to below the critical value for the copper in both cases of the RA and as-formed micro-pins. However, in the as-formed micro-pins (without RA), since the number of subgrains over the diameter (N_{SG}) is relatively higher than the critical value, no size effect has been seen. In comparison, for the RA micro-pins, since the number of subgrains

across the diameter is below the critical value, size effect is more significant on the mechanical behavior (Fig. 6-2-b).

Figure 6-6. Effect of post-production recovery annealing (RA) on the: (a) Mean grain size; and (b) Mean subgrain size. For the 0.3 mm micro-pins manufactured from CC11000 copper alloy strips initially annealed at different temperatures, before forming.

It has been proven that at the early stages of deformation, grain boundaries are the main obstacles for dislocation mobility [197]. In higher strains (or in a previously deformed metal), however, subgrain structure could also slow down the dislocation movement. Indeed, subgrain structures are considered as an array of dislocations, which could be an obstacle for dislocations' mobility, or at least delay the escape of dislocations through the specimen surface, especially when forming is done at low temperatures. Thus, the smaller the number of subgrains over the cross section (N_{SG}), the fewer will be the amount of obstacles for dislocations to escape out from the specimen surface, which in return decreases the flow stress.

This implies the importance of the number of subgrains rather than grains over the cross-section of the component for determining the size effect on mechanical behavior, which has not been addressed in literature by now. It can be also a reason for contradicting reports on size effects in the literature [51].

Table 6-1. Mean grain size, subgrain size, and number of grains and subgrains across the diameter of the 0.3 mm micropin in different pre- and post-production annealing conditions.

Pre- production annealing condition	Mean grain size (μm)		Mean subgrain size (μm)		N_G		N_{SG}	
	With RA	Without RA	With RA	Without RA	With RA	Without RA	With RA	Without RA
As- Received	5.4 ± 1.3	7.2 ± 2.1	4.3 ± 1.0	0.8 ± 0.2	56	42	70	375
400°C	13.0 ± 4.1	14.5 ± 3.4	11.3 ± 2.1	0.9 ± 0.3	23	21	27	333
800°C	47.0 ± 7.3	44.3 ± 6.7	38.2 ± 5.0	1.5 ± 0.4	6	7	8	200

* RA: Post-production recovery annealing at 230°C for annealing.

** N_G and N_{SG} are the number of grains and number of subgrains over the micro-pin's diameter, respectively.

6.3. Summary

The grain-specimen size effect was experimentally analyzed via examining micro-pins manufactured by an open-die micro-forging/extrusion. The proposed open-die forging/extrusion process is introduced as a microforming process which is not very sensitive to the in-process grain-specimen size effect. This makes this process more suited to be used for mass production of axi-symmetric micro-parts. It was experimentally shown that decreasing the number of grains over the thickness of the specimen will not always cause a size effect on mechanical behavior. Instead, the initial substructural dimensions must be considered. In this case, post-forming recovery annealing in coarse-grained micro-parts caused a deviation from the Hall-Petch equation in studying the mechanical properties of the final product, due to the coarsening of subgrain dimensions.

In fact, recovery annealing leads to annihilation of the dislocation structure and subgrain growth, especially for coarse-grained specimens. This increases the mean free path of the dislocations through the grains. Consequently, the flow stress decreases for the recovery annealed samples.

Chapter 7: Optimization of the Process; *Upper Bound Approach*

7.1. Introduction

To have a sustainable forming process, it is essential to optimize the process in the aspect of material wastage. Phenomenological studies in the previous chapters showed that there must be a critical blank thickness after which the extrusion is the dominant phenomenon, in these types of metal forming processes. This can be used for optimization of the process to reduce the material wastage and to reduce the required forming load. As explained in Chapter 2, using the upper bound theory has advantages over other analytical modeling techniques, in this matter.

7.1.1. Scope of this chapter

This chapter presents a developed upper bound solution for the optimization of open-die forging/extrusion processes in the forming of micro-parts from a sheet metal. Using such an analytical modeling approach, the critical blank thickness, the resulting final part geometry, together with the required forming

load is predicted based on the location of the neutral plane under the punch during the process. Indeed, the open-die forming processes can be controlled precisely by investigating and understanding about the location of the neutral plane during the process.

The effect of the punch-to-die diameter ratio is investigated, and a geometry size factor (x), related to the punch and die diameter, is introduced. In this chapter, all the work is reported in dry friction condition.

7.2. Upper bound theorem – basics

As mentioned in Chapter 2 section 2-5-2, basically the actual required forming power J^* ($\dot{W}$) is never higher than that computed by using Eq. (2-1). Referring to Eq. (2-1), the value for the $\dot{W}_s$ was considered as zero for this process [17].

Based on the Block Elemental Technique (BET), the half cross-section of the problem to be analyzed is schematically illustrated in Fig. 7-1-a. The zero point of the Y-R coordinate system is considered at center of the die orifice, in the horizontal coordination with the die surface. A blank forged between two parallel dies, one being flat, and the other being provided with an orifice (die orifice) at its center. There is no constraint on the side surfaces of the blank (considering a sheet metal as a blank).

Theoretically, the neutral plane at radius of R_n , is introduced to make a distinction between the two different material flow directions. Thus, the blank can be divided into two zones as illustrated in Fig. 7-1-a. Considering only Zone I (Compression or Upsetting zone), the forming condition is similar to a ring compression test for a ring with outer radius of R_0 and inner radius of R_n . The velocity field had to be slightly modified, as will be described in detail in section 7-3-1.

Figure 7-1. Schematic of the half cross section of the open-die forging/extrusion process; (a) division of the blank into two unit deforming zones based on the BET; (b) the definition of the critical blank thickness (when $R_i = R_n$).

In Zone II (Extrusion zone), extrusion is the dominant forming process occurring, with punch radius of R_n , and die radius of R_i . Here, the location of the neutral plane changes during forging of the blank, which literally is equal to the ring inner radius in the ring compression zone, and identical to the punch radius in the extrusion zone.

Theoretically, the initiation of the appearance of the neutral plane under the punch is the start of extrusion, since it indicates the material flow towards the die orifice. Before this stage, the dominant phenomenon is compression (upsetting) of the whole blank, meaning there is no or little material flow inside the die orifice. Once the neutral plane reaches the die entrance point ($R_i=R_n$), extrusion begins to be the dominant phenomenon (Fig. 7-1-b) and the speed of material flow inside the die orifice increases (Stage III as explained in Chapter 4, section 4-3-1). Indeed, after this Stage the share of the upsetting phenomenon will be less in the process, and there will be less material flowing outwards. Therefore, it can be assumed that the energy required to flow the material outwards of the punching area (the area under the punch) is negligible relative to the work done for pin-forming under the punch. This fact will be explained in more details in following sections.

Knowing that for a given punch and die diameter, the value of R_n depends only on the friction factor and workpiece thickness [17], a critical thickness can be found, after which the dominant phenomenon is extrusion. With knowledge of the critical blank thickness, the amount of material flowing outwards, and consequently, the wastage of the material in the process will be minimized.

Moreover, using the critical thickness determined for the process, it is possible to model and predict the final part dimensions and required process conditions.

As stated in Chapter 2, the following assumptions are made in applying the upper bound method:

- The deforming material is isotropic and incompressible, and the elastic deformation is neglected.
- The shear friction stress is considered constant at the die-blank interface during the process.
- The material follows the von-Mises flow rule.
- The effect of strain hardening, as well as the temperature effects, is neglected.
- The material flow outside the punching area is neglected, as by using the critical thickness, there is very limited flow outwards.

In addition, it was assumed that the location of the neutral plane is constant at any given time for a very small change in blank thickness, but varies at different steps during the process. The neutral plane, defined by $R=R_n$, is always a cylinder in 3-dimensions.

7.3. Analysis and formulation

7.3.1. Ring compression (Zone I)

Due to the symmetry of the ring compression process, the angular velocity v_θ was considered as zero. The vertical velocity of planes normal to the axis of symmetry, however, varies linearly with their distance from the die surface. The presumed velocity field is therefore as Eq. (7-1).

$$v_\theta = 0 \quad v_y = \frac{y}{T} v_p \quad v_R = f(R, y) \quad (7-1)$$

The resulting strain rates are

$$\dot{\epsilon}_{RR} = \frac{\partial v_R}{\partial R} \quad \dot{\epsilon}_{\theta\theta} = \frac{v_R}{R} + \frac{1}{R} \frac{\partial v_\theta}{\partial \theta} = \frac{v_R}{R} \quad \dot{\epsilon}_{yy} = \frac{\partial v_y}{\partial y} = \frac{v_p}{T} = \text{const.} \quad (7-2-a)$$

$$\dot{\epsilon}_{R\theta} = \dot{\epsilon}_{\theta y} = \dot{\epsilon}_{yR} = 0 \quad (7-2-b)$$

Assuming volume constancy for a given time,

$$\dot{\epsilon}_{RR} + \dot{\epsilon}_{\theta\theta} + \dot{\epsilon}_{yy} = \frac{\partial v_R}{\partial R} + \frac{v_R}{R} + C = 0 \quad (7-3)$$

which leads to

$$\frac{\partial v_R}{\partial R} + \frac{v_R}{R} + C = \frac{1}{R} \frac{\partial}{\partial R} (R v_R) + C = 0 \quad (7-4)$$

Therefore

$$v_R = -\frac{1}{2}CR + \frac{f(y)}{R} = -\frac{1}{2}\dot{\varepsilon}_{yy}R + \frac{f(y)}{R} \quad (7-5)$$

As mentioned before, at the position of neutral plane,

$$v_R \big|_{R=R_n} = 0 \quad (7-6)$$

Substituting the boundary condition into Eq. (7-5) results in

$$f(y) = \frac{1}{2}\dot{\varepsilon}_{yy}R_n^2 \quad (7-7)$$

Therefore

$$v_R = -\frac{1}{2}\dot{\varepsilon}_{yy}R \left[1 - \left(\frac{R_n}{R} \right)^2 \right] = -\frac{1}{2} \frac{v_p}{T} R \left[1 - \left(\frac{R_n}{R} \right)^2 \right] \quad (7-8)$$

Considering the inner radius of the ring equal to the neutral radius, and substituting all the velocity and strain rate fields into the Eq. (2-1) results in

$$\begin{aligned} \dot{W}_i &= \frac{2}{\sqrt{3}} \sigma_0 \int_V \sqrt{\frac{1}{2} \dot{\varepsilon}_{ij} \dot{\varepsilon}_{ij}} dV = -\frac{2}{\sqrt{3}} \sigma_0 2\pi T \frac{v_p}{T} \frac{1}{\sqrt{2}} \int_{R=R_n}^{R_0} \sqrt{\frac{1}{4} \left[1 + \left(\frac{R_n}{R} \right)^2 \right]^2 + \frac{1}{4} \left[1 - \left(\frac{R_n}{R} \right)^2 \right]^2} + 1 R dR \\ \Rightarrow \dot{W}_i &= -2\pi \sigma_0 v_p \int_{R=R_n}^{R_0} \sqrt{R^4 + \frac{1}{3} R_n^4} \frac{dR}{R} \end{aligned} \quad (7-9)$$

After integration, the $\dot{W}_i$ can be obtained as

$$\dot{W}_i = -\frac{\pi}{\sqrt{3}} \sigma_0 v_p R_n^2 \left\{ a(R_n) - 2 - \ln[b(R_n)] \right\} \quad (7-10)$$

where

$$a(R_n) = \sqrt{1 + 3 \left(\frac{R_0}{R_n} \right)^4} \quad (7-11)$$

$$b(R_n) = \left(\frac{R_n}{R_0} \right)^2 \frac{1 + \sqrt{1 + 3 \left(\frac{R_0}{R_n} \right)^4}}{3} \quad (7-12)$$

The expression for friction loss is as below (considering m as the friction factor between the die top-surface and the blank)

$$\dot{W}_f = \int_{s_L} \tau |\Delta v| ds = -\frac{2\pi}{\sqrt{3}} m \sigma_0 \frac{v_p}{T} \int_{R=R_n}^{R_0} R^2 \left[1 - \left(\frac{R_n}{R} \right)^2 \right] dR = -\frac{2\pi}{3\sqrt{3}} m \sigma_0 \frac{v_p}{T} R_n^3 \left[2 + \left(\frac{R_0}{R_n} \right)^3 - 3 \left(\frac{R_0}{R_n} \right) \right] \quad (7-13)$$

Therefore

$$J_r^* = \dot{W}_i + \dot{W}_f = -\frac{\pi}{\sqrt{3}} \sigma_0 v_p R_n^2 \left\{ a(R_n) - 2 - \ln[b(R_n)] + \frac{2mR_n}{3T} c(R_n) \right\} \quad (7-14)$$

where

$$c(R_n) = 2 + \left(\frac{R_0}{R_n} \right)^3 - 3 \left(\frac{R_0}{R_n} \right) \quad (7-15)$$

7.3.2. Extrusion (Zone II)

As can be seen in Fig. 7-2, Zone II itself can be divided into three different blocks. In Block A, there is energy loss regarding both friction and internal deformation ($J_{e,A}^* = \dot{W}_i + \dot{W}_f$); while in Block B, it was assumed that there is no energy loss ($J_{e,B}^* = 0$), as the material is only pushed towards the die orifice. In Block C however, there is only friction energy loss ($J_{e,C}^* = \dot{W}_f$) and it is assumed that no internal deformation occurs while material flows inside the die orifice.

From [154], at a given time, in Block A it is assumed that

$$v_\theta = 0 \quad v_y = \frac{y}{T} v_p \quad v_R = -\frac{1}{2} \frac{v_p}{T} R \left[1 - \left(\frac{R_n}{R} \right)^2 \right] \quad (7-16)$$

because the material in Block A deforms as similar as a ring compression test.

Whereas in Block B

$$v_\theta = v_R = 0 \quad v_y = \frac{y}{T} v_p \quad (7-17)$$

Figure 7-2. Schematic of different blocks in the Zone II. (Blocks A, B and C are segments of the extrusion Zone divided based on BET)

It is to be noted here that since the vertical velocity of Blocks A and B in Zone II (Eq. 7-16 and Eq. 7-17) were assumed equal to the vertical velocity of the Zone I ($v_y = \frac{y}{T} v_p$ in Eq. 7-1), therefore, the shear loss between these blocks were neglected.

In Block C, the velocity just before the die orifice is $v_{y1} = v_p$. Assuming volume constancy at a given time, the velocity inside the die orifice would be calculated as

$$\pi R_i^2 v_{y2} = \pi R_n^2 v_p \xrightarrow{\text{yields}} v_{y2} = \left(\frac{R_n}{R_i} \right)^2 v_p \quad (7-18)$$

Therefore, the velocity change due to the material flow inside the die orifice is defined as Eq. (7-19)

$$\Delta v = v_{y2} - v_p = v_p \left[\left(\frac{R_n}{R_i} \right)^2 - 1 \right] \quad (7-19)$$

To calculate the friction energy loss rate for the extrusion zone, it is necessary to consider the effect of the dead-metal-zone on the process. Based on the calculation in [17], there is a conic dead-zone being formed on the die surface in the extrusion process. The angle of this dead-zone (defined as the optimal cone angle in section 8.3.6 and 8.3.7 of Ref. [17]), is calculated based on the upper bound theory as Eq. (7-20). The expanded equations can be found in the Appendix, section A-2.

$$\alpha = \sqrt{\frac{3}{2} m \ln(R_n / R_i)} \quad (7-20)$$

Therefore, the internal power of deformation for Zone II is expressed as Eq. (7-21), which is a combination of Eq. (8.11a) in Ref. [17] and Eq. (7-18) in this study.

$$\dot{W}_i = 2\pi\sigma_0 v_p R_n^2 f(\alpha) \ln \left(\frac{R_n}{R_i} \right) \quad (7-21)$$

where

$$f(\alpha) = \frac{1}{\sin^2 \alpha} \left(1 - \cos \alpha \sqrt{1 - \frac{11}{12} \sin^2 \alpha} + \frac{1}{\sqrt{132}} \ln \frac{1 + \sqrt{\frac{11}{12}}}{\sqrt{\frac{11}{12}} \cos \alpha + \sqrt{1 - \frac{11}{12} \sin^2 \alpha}} \right) \quad (7-22)$$

Based on Eq. (10.5) in Ref. [17], the shear friction loss along surfaces of velocity discontinuities becomes:

$$\dot{W}_{f1} = \frac{2\pi}{\sqrt{3}} \sigma_0 v_p g(\alpha) R_n^2 \quad (7-23)$$

which expresses the shear loss due to change in the cross-section area, along the dead-zone interface. Here,

$$g(\alpha) = \frac{\alpha}{\sin^2(\alpha)} - \cot(\alpha) \quad (7-24)$$

Friction loss along the die surface, under the dead-zone area, is defined as:

$$\dot{W}_{f2} = \frac{2\pi}{\sqrt{3}} m \sigma_0 v_p \ln \left(\frac{R_n}{R_i} \right) h(\alpha) R_n^2 \quad (7-25)$$

where

$$h(\alpha) = \cot(\alpha) \quad (7-26)$$

The friction loss inside the die orifice could be defined as below

$$\dot{W}_{f3} = \frac{2\pi}{\sqrt{3}} m \sigma_0 v_p \frac{R_n}{R_i} R_n^2 \quad (7-27)$$

Since the metal is in contact with the die wall inside the die orifice, the effect of the die landing length must be considered in modeling. It was considered that the die land length (refer to Fig. 7-1-a for definition of the land length) is less than the final pin height inside the orifice. In this case, Ajiboye and Adeyemi [148] reported that there is an effective land length in which the tooling-material interface participates in the friction energy loss. The material-die contact after this length has no or limited effect on the energy loss. In this work, based on [141], the normalized effective die land length is assumed as $\frac{L}{R_n} \approx 1$ (it is to be noted that $R_{0 \text{ (Ajiboye's work)}} = R_n \text{ (Present work)}$). Therefore, the effective land length (see Fig. 7-1-a) in the present work was considered as $L = R_n$, in which R_n changes during the forming process.

The other important factor to be considered was friction. Becker *et al.* [20] reported that using the coulomb friction model sometimes can lead to a better solution in metal forming modeling [72]. However, using this method, the value of the local normal pressure at the interface needs to be known during the process. Due to the complexity of the material flow inside the die orifice, it is impossible to predict the normal pressure during the process. Therefore, shear friction factor was considered, as suggested by Avitzur [17].

Therefore, the total energy loss for the Zone II is defined as Eq. (7-28)

$$J_e^* = \dot{W}_i + \dot{W}_f = 2\pi\sigma_0 v_p R_n^2 \left[f(\alpha) \ln\left(\frac{R_n}{R_i}\right) + \frac{\alpha}{\sqrt{3}} + \frac{m}{\sqrt{3}} \left(\frac{R_n}{R_i}\right) + \frac{mh(\alpha)}{\sqrt{3}} \ln\left(\frac{R_n}{R_i}\right) \right] \quad (7-28)$$

7.4. Solution procedure

7.4.1. Geometry size factor

The total energy rate in the process must be calculated by the summation of the two energy consumed in extrusion and compression (upsetting) phenomena.

However, by decreasing the die-to-punch diameter ratio, the process becomes more similar to the disk compression test, and it becomes more difficult for the material to flow inside the die orifice. Therefore, a factor must be implemented to consider the share of each phenomenon (upsetting and extrusion) on the overall total energy consumption. To do so, based on the location of the neutral plane, a geometry size factor of x was considered for weighting. Assuming the volume constancy for the material under the punch, the extrusion fractional volume of the material under the punch at a given time (*Workpiece thickness=const.*) is defined as (see Fig. 7-1-a):

$$x = \frac{\pi R_n^2 T}{\pi R_0^2 T} = \left(\frac{R_n}{R_0} \right)^2 \quad (7-29)$$

It is to be noted that the value of x is directly related to the value of the location of the neutral plane and therefore, changes during the process.

The share of the upsetting (compression) phenomenon would be $1-x$, accordingly. Therefore the total energy consumption can be defined as:

$$J^* = (1-x)J_r^* + xJ_e^* = \pi\sigma_0 v_p R_n^2 \left\{ x \left(\left(2f(\alpha) + \frac{2mh(\alpha)}{\sqrt{3}} \right) \ln \left(\frac{R_n}{R_i} \right) + \frac{2g(\alpha)}{\sqrt{3}} + 2m \frac{R_n}{R_i} \right) + \frac{1-x}{\sqrt{3}} \left(-a(R_n) + 2 + \ln(b(R_n)) - \frac{2mR_n}{T} c(R_n) \right) \right\} \quad (7-30)$$

As the value of x is defined based on the geometrical conditions in the process, this coefficient was named *geometry size factor*.

7.4.2. Location of the neutral plane

Since the total forming energy rate calculated by the upper bound technique is never less than that of actual one, the solution can be determined by minimizing J^* . Taking the values of R_0 , R_i and m , and minimizing Eq. (7-30) with respect to R_n , the most suitable position for the neutral plane (R_n) can be found for each blank thicknesses.

Using MATLAB v. 7.8.0.347(R2009), differentiating the total energy with respect to the R_n , and equalizing it with zero results in:

$$\frac{\partial J^*}{\partial R_n} = 0 \xrightarrow{\text{yields}} \frac{R_0}{T} = f(R_n, R_i, m) \quad (7-31)$$

Using Eq. (7-31), the optimum R_n value can be found for a specific geometrical condition. Details can be found in Appendix (Eq. A.3).

7.4.3. Forming pressure

Knowing the location of the neutral plane, the required forming power supplied by the punch through the upper platen to the blank is defined as:

$$J^* = P v_p \quad (7-32)$$

Therefore, the relative mean pressure in the direction of punch over the projected area of the blank on the plane perpendicular to the direction of punching can be derived as:

$$\frac{p_{avg}}{\sigma_0} = \frac{P}{\pi R_0^2 \sigma_0} \quad (7-33)$$

Therefore, from Eq. (7-30), Eq. (7-32) and Eq. (7-33),

$$\frac{p_{avg}}{\sigma_0} = \left[x \left(\left(2f(\alpha) + \frac{2mh(\alpha)}{\sqrt{3}} \right) \ln \left(\frac{R_n}{R_i} \right) + \frac{2g(\alpha)}{\sqrt{3}} + 2m \frac{R_n}{R_i} \right) \right] \left(\frac{R_n}{R_0} \right)^2 \quad (7-34)$$

$$+ \frac{1-x}{\sqrt{3}} \left(-a(R_n) + 2 + \ln(b(R_n)) - \frac{2mR_n}{T} c(R_n) \right)$$

Knowing the location of the neutral plane, the average forming pressure required for extrusion/forging process with defined punch and die and blank geometry is predicted by Eq. (7-34). Details can be found in Appendix (Eq. A.4).

7.4.4. Final part dimensions

In addition to the required forming pressure (and thus load), the achievable pin height for different process conditions could be used as the calibration parameter for the modeling. To calculate the final pin height, the amount of material flowing towards the die orifice must be calculated. Assuming the workpiece as a ring with radius of R_i , this amount of material can be considered as the lateral “*bulging*” of material in the ring compression process. Avitzur [17] calculated the volume of bulging as radial dimensional change by considering the velocity of material during the process. In our study, the horizontal “*bulging*” material was assumed to be pushed inside the die orifice when the stage of the process was dominated by the extrusion mechanism (Fig. 7-3).

Figure 7-3. Schematic of the die-set showing the *bulging* occurring during the process for a small punch stroke (It is assumed that the bulging region with thickness of ΔR will be just pushed inside the die orifice during the process).

From the definition of the velocity ($v = \frac{\text{distance}}{\text{time}}$), it is concluded that at a very small punch displacement ($\Delta T = T_2 - T_1$ according to Fig. 7-3)

$$\Delta t = \frac{\Delta T}{v_p} \quad (7-35)$$

On the other hand, since the R_0 is a constant value for a defined process geometry, therefore

$$\Delta \left(\frac{T}{R_0} \right) = \frac{\Delta T}{R_0} = \text{const.} \xrightarrow{\text{yields}} \Delta T = CR_0 \quad (7-36)$$

$$\Delta t = \frac{CR_0}{v_p} \quad (7-37)$$

As described in Chapter 7 of [17], $\Delta R = v_R \Delta t$

Combining with Eq. (7-37) and considering the v_R (Eq. (7-16)) at the die entrance ($R = R_i$), it follows that

$$\Delta R = -\frac{1}{2} \frac{v_p}{T_2/2} R_i \left[1 - \left(\frac{R_n}{R_i} \right)^2 \right] \frac{CR_0}{v_p} = -\frac{CR_0 R_i}{T_2} \left[1 - \left(\frac{R_n}{R_i} \right)^2 \right] \quad (7-38)$$

where T_2 is the subsequent thickness after the punch displacement of ΔT . The bulging volume was considered to flow into the orifice and be added to the pin height, therefore, assuming volume constancy at a given time,

$$T_2 \left[R_i^2 - (R_i - \Delta R)^2 \right] = R_i^2 h_1 \quad (7-39)$$

$$\Rightarrow T_2 \left[R_i \Delta R - \Delta R^2 \right] = R_i^2 h_1 \quad (7-40)$$

Combining Eq. (7-38) and Eq. (7-40) yields:

$$\frac{h_1}{R_i} = -\frac{T_2}{R_i} \left\{ \frac{\Delta T}{T_2} \left[1 - \left(\frac{R_n}{R_i} \right)^2 \right] \left[1 + \frac{\Delta T}{T_2} \left[1 - \left(\frac{R_n}{R_i} \right)^2 \right] \right] \right\} \quad (7-41)$$

It is important to note that the material in Block B of Zone II will be just pushed inside the die orifice, resulting in $h_2 = \Delta T$. Therefore, the total theoretical pin aspect ratio is defined as

$$\frac{h_t}{R_i} = \frac{h_1 + h_2}{R_i} \quad (7-42)$$

7.5. Modeling results

To obtain the value of the geometry size factor in the model, the variation of the punch load and the pin aspect ratio versus punch stroke is compared with the experimental results. The results were also compared to the other report [154], using the same process conditions.

From [142], the friction factor varies during the ring compression test. It is reported that for dry conditions (when $R_0 \leq R_n \leq R_i$), the value of friction factor, m , must be considered as nearly 1. Hence, in this work, the value of 1 was considered as the friction factor. As discussed, friction factor has a direct effect on the formation of the dead metal zone and the frictional energy loss during the forming process. Therefore, changing the friction factor (i.e.

lubricated condition) has a significant effect on the modeling results, which is being investigated.

Considering the boundary conditions explained above and using Eq. (A.3), the calculated location of the neutral plane (the distance from the axis of symmetry) is illustrated in Fig. 7-4 for each pin-to-punch diameter ratio. As can be seen, there is a starting point for each curve, before which the process geometrical conditions did not conform to the modeling boundary conditions.

Figure 7-4. The calculated location of the neutral plane radius for different punch and die diameters based on the upper bound theory. Friction factor m assumed 1 and the

value of x equal to $\left(\frac{R_n}{R_0}\right)^2$.

Based on Fig. 7-4 and Eq. (A.3), the critical blank thickness can be plotted, as shown in Fig. 7-5. Knowing the punch radius (R_0) and die radius (R_i), the thickness after which the extrusion is going to be the dominant phenomenon, can be easily found by using this curve.

As can be seen in Fig. 7-5, in general, keeping the punch radius constant and reducing the die radius leads to a thicker critical blank thickness.

Indeed, the value of critical thickness shows the point after which the pin height increases sharply during the pin forming. These optimum points were reported experimentally in the Chapter 4. The model was validated also with the results extracted from the Vickery and Monaghan work [154], which showed a reasonable fit to the modeling outcome.

Figure 7-5. Critical blank thickness for the forging/extrusion process using upper bound approach.

Having the values of the neutral plane radius for each punch and die diameters, the forming pressure was plotted in Fig. 7-6 using Eq. (A.4). It is important to note that the calculated pressure is valid for the forming process using the sheet metal with a critical thickness. As can be seen in Fig. 7-6, increasing the die-to-punch diameter ratio leads to increase in the forming pressure. A similar trend has been reported before in [147], although the friction energy loss inside the die orifice was not considered in his work. Indeed, as the die radius becomes smaller, there is more work (pressure) needed to push (flow) the material inside the die orifice (geometry size effect). As can be seen, there is a nice fit between experimental and modeling results, which was also validated by simulation in Chapter 4.

Figure 7-6. Calculated mean forming pressure versus punch displacement after critical blank thickness, for various die-to-punch diameter ratios.

Using Eq. (7-41) and Eq. (7-42), the pin aspect ratio for different die-to-punch diameter ratios was plotted in Fig. 7-7. The experimental results showed a reasonable fit. Any discrepancy is in reasonable range and could be because of experimental conditions, such as galling that occurs during the process, as described in Chapter 4, section 4-2-1. These results were validated also by calculations interpreted from [154]. These results are shown in Table 7-1.

It is important to note that the curves in Fig. 7-7 must be analyzed considering the normal pressure of the process (Fig. 7-6). In other words, the high values of the $h/2R_i$ in Fig. 7-7 might not be physically achievable, especially for higher values of R_i/R_0 . Thus, the higher values for the $R_i/R_0=0.4$ and $R_i/R_0=0.6$ is only for showing the theoretical trend.

Table 7-1. Pin forming results obtained from an open-die forging/extrusion process, calculated from [154].

Punch radius (mm)	Pin radius (mm)	Critical thickness (mm) {interpreted from [154]}	Final thickness (mm) {interpreted from [154]}
12.5	7.5	2.5	1.5
12.5	5	4	2.5
12.5	2.5	6.25	3

Figure 7-7. Calculated pin aspect ratios during the forging/extrusion processes for

different $\frac{R_i}{R_0}$ values, validated by experimental results.

7.6. Summary

The upper bound approach was developed for the optimization of the forming processes in view of reducing material wastage. This approach is successfully derived for optimization of the open forging/extrusion processes, regardless of the metal used in the process. Thus, three important parameters of the process were predicted: (i) the critical blank thickness for manufacturing axi-symmetric parts using forging/extrusion processes; (ii) the forming pressure (load) required for forming of such a product; (iii) the final part geometry achievable. A *geometry size* factor was proposed considering the relative effect of the location of the neutral plane and punch diameter on the energy loss during the process in a dry condition. All these calculations were based on precise prediction of the optimum location of the neutral plane under the punch, which changes during the process.

The effects of material flow inside the die orifice on the total energy was considered using the optimum land length which was equal to the R_n . This way, the effect of process geometry and also the effect of location of the neutral plane on the material flow inside the die orifice were considered in the model. This again implies the important role of the neutral plane location, which must be considered for accurate modeling of the open-die forming processes.

The effect of different type of lubricants on the overall material behavior and on the modeling results is going to be the topic of the next chapter.

Chapter 8: Lubricant Effects

8.1. Introduction

In micro-forming processes, it is preferred not to use lubricants, due to their complex behavior in micro-scale [114]. Nevertheless, this made the topic attractive to the scientific society and has led to a vast number of related reports about lubrication size effects [102, 107, 120, 198].

To complete our study, following the analytical modeling in the last Chapter that provided a universal solution for the process in dry condition, it is necessary to develop the solution for different lubrication conditions. This could also help in a deeper understanding of the in-process material behavior.

8.1.1. Scope of this chapter

The aim of this chapter is to study the effect of lubricants with different friction factors on the material behavior during the process. To do so, two different liquid lubricants having different viscosities, together with a solid lubricant were used. After studying the friction effects on the final part

dimension and the required forming load, the developed Upper Bound model was justified to elaborate the friction effects on the solution. Two approaches were chosen for identifying the friction factor in the model: (i) global friction factor, (ii) localized friction factor. Comparison of the modeling results with the experimental showed the reliability of the second approach, providing a better fit.

At the end of this study, the lubricant effect on the DMZ behavior during the process was elaborated as well.

8.2. Friction factor

The result of the ring-compression test was used to determine the friction factor for different lubricants, as shown in Fig. 8-1. In this figure, the finite element results (using DEFORM 2D) are shown by lines compared to the data points achieved from the experiment.

Extracted from Fig. 8-1, Table 8-1 represents the shear friction factor for each lubricant. It is to be noted that Wisura and Fuchs were used in this study as two liquid lubricants with different viscosities as mentioned in Chapter 3. Teflon film was used as a solid (semi-solid) lubricant. The Teflon film was adhered thermally to the die surface by heating the die up to 150°C.

Figure 8-1. Determination of the shear friction factor using ring compression test and finite element simulation.

Table 8-1. Shear friction factor for various lubricants determined using ring compression test.

Lubrication Condition	Non (Dry)	Wisura	Fuchs	Teflon
Shear Friction Factor	1	0.55	0.3	0.25

8.3. Friction effects on the part dimension and forming load

Figure 8-2 represents the effect of different lubrication conditions on the final pin aspect ratio manufactured by the progressive microforming process. To eliminate the punch diameter effect, a constant punch diameter of 2.0 mm was chosen for manufacturing of the micro-pins. To ensure about the outcome,

the lubricant effects were investigated on different pin diameters from micrometer (0.3 mm) to millimeter range (1.2 mm).

Considering the standard deviations, no significant lubricant effect on the pin dimension can be seen in Fig. 8-2. This behavior goes back to the nature of the process, which will be elaborated in sections ahead.

There is, however, a difference in the required forming load by using different lubricants. Figure 8-3 shows a typical punch load-stroke behavior for the 0.3 mm pin manufactured by 2.0 mm punch using various lubricants. Generally, a decrease in the friction factor leads to a drop in the forming load. The difference in the forming load is relatively larger in Stage (II) of the diagram (where is responsible for the compression phenomenon).

Figure 8-2. Friction effects on the final pin dimensions for various pin diameters manufactured by the 2.0 mm punch.

Figure 8-3. Friction effects on the forming load behavior during production of the 0.3 mm pin using 2.0 mm punch.

Indeed, it has been proven that especially in open-die forming processes, in which the lubricant could have a way to escape out, the lubricant effect is more significant at the beginning of the process (low normal pressures) [1, 2].

In case of solid lubricants, as shown in Fig. 8-4, the Teflon film was torn off during the process. Consequently, there was no lubricant that existed at the blank-die interface, especially during the last Stage of the process.

It is important to note that the same behavior was seen in case of using different punch diameters, for manufacturing the 0.3 mm pin.

Figure 8-4. Teflon layer tear off during the first stages of the process, showing a weak lubrication effects on the part forming.

To ensure the effect of solid lubricants inside the die orifice, the Teflon film was thermally coated inside the die orifice and also on the die surface. To do so, the die diameter must be big enough to be accessible. Thus, the 1.2 mm die was selected and heated up to 150°C. After that, a 100 μm thick solid film of Teflon was adhered on the whole surface of the die, including inside the die orifice. This provides a well-adhered layer of Teflon on the die surface and inside the orifice.

After pin forming, although there was Teflon tear off seen on the die surface, the Teflon film inside the die orifice affected the material behavior during the process, as depicted in Fig. 8-5. Due to effective lubrication inside the die orifice, the pin height was increased. This implies the importance of the lubricant inside the die orifice.

Figure 8-5. Effect of solid lubricant (Teflon), coated on different part of the forming tool, on the final pin aspect ratio for the 1.2 mm pin manufacture by the 2.0 mm punch.

However, checking with related industries, it is technically difficult to coat the Teflon inside the die orifice by any method, especially as the die orifice gets smaller in diameter. Thus, it was impossible to investigate the effect of solid lubricants inside the die orifice for smaller die diameters in micro-scale. Nevertheless, the key role of the friction inside the die orifice on the material behavior has been shown via FE simulation in Chapter 4, Section 4-7-3. It is worth noting that applying lubricants on the punch surface had a negligible effect on the material behavior in the process.

8.4. Upper-Bound solution in different lubrication condition

The developed Upper Bound model was justified for different lubrication conditions. To do so, the critical thickness was firstly calculated for production of the 0.3 mm micro-pin using 2.0 mm punch. Based on Fig. 7-5 and Eq. (A.3), the critical thickness was found to be roughly 0.56 mm. This value was used as the initial blank thickness in the experiments for the whole Section 8-4 (to validate the modeling results). It is to be stated that, as mentioned in Chapter 7, Section 7-5, the effect of friction on the calculation of the critical thickness in this process is negligible. Therefore, it was assumed that the critical thickness is the same for both dry and lubricated conditions. The next step would be changing the friction factor in the model, for which two approaches were selected as being described in the following sections.

Important to state that the hybrid friction model used in Chapter 3 could not be used for the modeling since it is impossible to calculate the “localized” normal pressure numerically; thus, the shear friction model was used.

8.4.1. Approach one: Global friction factor

One approach could be just to change the global friction factor in the model according to Table 8-1. Figures 8-6-a and 8-6-b show the result for the required forming pressure and calculated pin height, respectively. As can be seen, there is a discrepancy between experimental and modeling results for different

lubricants. Thus, this approach could not be considered as a logical approach for the process under the mentioned circumstances.

Figure 8-6. “Global-Friction” Upper Bound solution for: (a) the mean forming pressure, and (b) the pin dimension; versus punch displacement for the 0.3 mm pin manufactured by the 2.0 mm punch using different lubricants.

8.4.2. Approach two: Localized friction factor

The above-mentioned miss-match could be due to the fact that the friction factor was considered globally the same throughout the process interfaces. However, as discussed in Chapter 4, Section 4-3-1, the material has different behavior during various process Stages. Therefore, in another approach, as schematically shown in Fig. 8-7, two different friction factors were considered for different regions in the deforming material during the process.

Figure 8-7. Schematic half-cross-section of the forming process showing the definition of the localized friction factor, based on the phenomenological studies.

Based on our observations (Fig. 8-3 and Section 4-3-1 in Chapter 4), the lubricant effect is less during the last Stage of the process (Extrusion Stage). Thus, the hypothesis was defined as almost no lubricant effects for Zone II (Extrusion Stage). Therefore, the shear friction factor was assumed as equal to 1

for Zone II in all lubrication conditions, while the friction factor for Zone I was defined according to Table 8-1 for each lubricant. The m value in Eq. (7-1) to Eq. (7-15) was considered as m_1 equal to respective values in Table 8-1, and the m value in Eq. (7-16) to Eq. (7-28) was assumed as $m_2=1$.

Figure 8-8. “Localized-Friction” Upper Bound solution for: (a) the mean forming pressure, and (b) the pin dimension, versus punch displacement for the 0.3 mm pin manufactured by 2.0 mm punch using different lubricants.

As shown in Fig. 8-8, there is a good fit between experimental and modeling results using the localized friction factor (approach two). This proves the hypothesis of using localized friction factor.

Consequently, the friction behavior in modeling of the forming process must be considered based on the phenomenological studies on the material behavior during forming.

Comparison of the Fig. 8-6-b and Fig. 8-8-b indicates that decreasing the friction factor inside the die orifice, as well as on the die surface could lead to a relatively significant increase in the final pin height. This was experimentally shown in Fig. 8-5 for the 1.2 mm pin.

8.4.3. Comparison of the two approaches; location of the neutral plane

Figure 8-9 shows the effect of the two different approaches of using (i) global friction (Fig. 8-9-a), and (ii) localized friction (Fig. 8-9-b) on the location of the neutral plane, using Eq. (A.3) for the 0.3 mm pin manufactured by 2.0 mm punch.

Comparatively, using the localized friction approach (Fig. 8-9-b), the relative location of the neutral plane for a defined thickness decreases by using lubricants. This implies that by decreasing the friction at the die surface, the neutral plane moves very slightly towards the die orifice. This marginal behavior has been also proven by finite element simulation in Chapter 4 (See Fig. 4-22), again satisfying the reliability of the localized friction hypothesis.

Figure 8-9. Calculated location of the neutral plane during the production of the 0.3 mm pin using 2.0 mm punch for different lubricants. (a) global friction solution; (b) localized friction solution.

This behavior is, however, not seen for the case of the global friction factor in Fig. 8-9-a, since the friction factor was considered globally. As mentioned in Chapter 4, a decrease in friction inside the die orifice (Zone II) leads to an

increase in the relative location of the neutral plane. However, as discussed, this could not be achieved experimentally by using lubricants, as it is difficult to push and keep the lubricant inside the die micro-orifice.

8.5. Friction effects on the DMZ behavior; microstructural studies

After analytical studies on the lubricant's effect, it would be interesting to investigate the effect of lubricant on the behavior of the unique Dead Metal Zone (DMZ) during the process. To do so, based on the pre-discussed observation in Chapter 4 (Fig. 4-12), the lubricants' effect on the microstructure of the 0.3 mm pin manufactured by the two punches of diameters of 2.0 and 3.2 mm was investigated, as illustrated in Fig. 8-10. In the investigations, initial blank thickness was chosen as 2.5 mm; because using the optimum blank thickness of 0.56 leads to prevention of the DMZ formation at the pin surface (Chapter 5, Section 5-4), making it impossible to monitor the DMZ behavior.

As can be seen, lubrication has no effect on the DMZ behavior in the pin microstructure: using the 3.2 mm punch, there is a narrow layer of DMZ at the pin surface for all the lubrication conditions; there was no DMZ layer at the pin surface for micro-pins manufactured by the 2.0 mm punch.

Figure 8-10. Lubrication effects on the microstructural (DMZ) behavior of the material during the production of the 0.3 mm pin using two different punch diameters: (i) 3.2 mm punch and various lubrication conditions of: (a) Dry, (b) Wisura, (c) Fuchs, (d) Teflon; and (ii) 2.0 mm punch 2.0 mm using various lubricants of: (e) Dry, (f) Wisura, (g) Fuchs, (h) Teflon. The microstructure has been depicted at the left and the microhardness profile at the right side of each image. Dashed line indicates the identification of the DMZ based on the grain shapes in the microstructure.

However, as can be seen, by decreasing the friction factor for each punch diameter (from Fig. 8-10-a to 8-10-d, and from Fig. 8-10-e to 8-10-h), the volume of the DMZ in the head part decreases. This is associated to the conic angle of the DMZ in the head part described by Eq. (7-20). In that equation, the angle of the DMZ in the head part (which corresponds to the relative volume of the DMZ at the head part) is related to the square root of the friction factor. Thus, by decreasing the friction factor at the die surface, the conic angle of the DMZ (and consequently, its volume) decreases.

Nevertheless, since the other parameters, such as grain size and the process geometry is the same, there is no significant effect on the leakage of the DMZ to the pin surface.

8.6. Summary

The lubrication effects on the material behavior were studied experimentally and analytically. It was shown that the lubrication in this manner has no significant effects on the dimension of the pins, but has marginal effects on the required forming load. This unique behavior was investigated using the developed upper bound model. It was proven that the friction factor must be considered locally rather than a global friction factor, to get a reasonable validation for the model. In fact, the liquid lubricant could not stay inside the die orifice, and it was easily squeezed out from the die wall surface. The lubrication behavior on the die surface was assumed as normal. The comparison of the experimental and modeling results proved the reliability of the mentioned hypothesis. It was theoretically shown that a successful lubrication inside the die orifice could increase the final part dimension significantly. The only suggestion would be using the solid coatings. However, the challenge that we faced was the technical difficulty in coating inside the die orifice with a diameter in the micro-scale.

Chapter 9: Conclusions

9.1. Experimental Study and Finite Element Simulation on the Material Flow

The feasibility of the progressive microforming process for fabrication of micro-pins without any defect was proven. From the microstructure study, it was identified that there are regions at the head part, which are less deformed as compared to other parts. These zones are known as Dead Metal Zones (DMZs). The leakage of the material of the DMZ into the extrusion was the main cause of the inhomogeneous mechanical properties in the pin. A systematic experimental and FE simulation study was carried out to explore the effect of process parameters on the microstructure evolution, which provided the following novel outcomes:

1. FE simulation results showed that there are two types of material flow in the head; one towards the die orifice, and one directed away from the die orifice. This leads to the formation of a “mobile” neutral plane under the punch which separates the two material flows. Coupling FE simulation results with experimental results showed that the crossing of the neutral plane and DMZ in the head part can be used as one indicator on whether the existence of the DMZ at the pin surface will occur. Therefore, by using FE simulation to identify the location of the neutral zone, and changing the

process condition accordingly, the material flow can be controlled during the progressive forming process and the leakage of the DMZ into the extruded pin can be prevented, if desired.

2. Phenomenological investigation revealed that the progressive microforming process is a type of open-die forging/extrusion process, which has two main phenomena involving; (i) Upsetting and (ii) Extrusion. Changing the frictional conditions for each of the phenomenon involved in the process has different effects on the material performance behavior. It was shown that to get the optimum pin dimension, higher friction on the die top surface, together with the minimum friction inside the die orifice must be applied.
3. By decreasing the die diameter, the friction factor was increased. This is related to the size effect in the process, as by decreasing the die diameter, the fraction of area to the volume of the material inside the die orifice increases. This fact was quantified for different geometrical conditions in the process using ANOVA analysis.

9.2. Grain Size and Workpiece Dimension Effects

The grain size effect together with the thickness size effect in the process was experimentally analyzed. A systematic study was planned to monitor the

microstructural size effect using EBSD technique. The following revelations were obtained:

1. With respect to the in-process mechanical response of the metal and also the final part dimensions, open-die forging/extrusion processes can be introduced as processes which are not very sensitive to the grain size effect. This is another remarkable advantage of this technique compared to existing microforming processes, making this process more suited to be used for mass production. Indeed, the location of the neutral plane and its development throughout the specimens play the key role, regardless of the pre-production properties of the raw material.
2. Decreasing the number of grains over the thickness of the specimen, however, affects the microstructural evolution during the process. By increasing the initial grain size, the relative volume of the DMZ at the head part decreases. In coarse-grained specimens, there is a delay in development of DMZ due to a larger dislocation cell size in the microstructure. The smaller the size of the DMZ at the head part, the probability of the DMZ to be pushed towards the die orifice is reduced. Thus, increasing the initial grain size can be used as an alternative solution for preventing the DMZ leakage to the pin's microstructure. Other side-effects of having larger grain size, such as decreasing the mechanical strength of the pins, and possible increase in the wearing and galling effects inside the die orifice must be considered.

3. It was found that the fractional share of the upsetting phenomenon on the overall process was decreased for the pins manufactured from thinner strips. This is so because decreasing the strip thickness results in the earlier development of the neutral plane under the punch. As a result, the metal would mostly flow towards the die orifice at the early stages. This implies that there must be a critical strip thickness, after which the metal flows mainly towards the die orifice and the pin forms mainly afterwards.
4. Examining the post-production size effects, it was shown that decreasing the number of grains over the pin diameter will not always cause a size effect on post-production mechanical behavior. Instead, the relative substructural dimensions must be considered. In this case, recovery annealing in coarse grained micro-parts caused a deviation from the Hall-Petch equation in studying the mechanical properties, due to the coarsening of subgrain dimensions.

9.3. Analytical Modeling; Process Optimization

Based on the phenomenological studies, the upper bound approach was modified for the optimization of the forming processes in view of material wastage. The model is based on the minimization of the required forming energy with respect to the location of the neutral plane. The block elemental technique was used to divide the deforming material into different blocks for simplification of the modeling procedure. This approach was successfully

derived for optimization of the open forging/extrusion processes, regardless of the metal type used in the process. The following conclusions were obtained:

1. Three important parameters of the process are predicted using the developed model: (i) the critical blank thickness for manufacturing axis-symmetric parts using forging/extrusion processes; (ii) the forming pressure (load) required for forming of such a product; (iii) the final part geometry achievable. This makes us able to control and predict the in-process material behavior based on the desired final part dimensions.
2. A *geometry size* factor was proposed considering the relative effect of the location of the neutral plane and punch diameter on the energy loss during the process. This factor also explains the friction factor size effect evaluated via the FE simulation. All these calculations were based on precise prediction of the optimum location of the neutral plane under the punch, which changes during the process.
3. The effects of material flow inside the die orifice on the total energy were considered using the optimum die land length. This way, the effect of process geometry and also the effect of location of the neutral plane on the material flow inside the die orifice were considered in the model. This again implies the important role of the neutral plane location, which must be considered for accurate modeling of the open-die forming processes.

4. It was shown that for a constant die diameter, increasing the punch diameter leads to increase in the required (critical) blank thickness. However, increasing the punch diameter for a constant die diameter increases the required forming pressure. This must be considered in process optimization, based on the forming press capacity.

9.4. Lubricant Effects

The lubrication effects on the material behavior were studied experimentally and analytically. Two different liquid lubricants with distinct viscosities were used. Teflon film was also used as a solid lubricant. The below outcomes were extracted:

1. It was shown that the liquid lubrication has no significant effects on the final pins' dimension, but has marginal effects on the required forming load. This is because the liquid lubricant could not be pushed and be kept inside the die orifice; instead, it was being squeezed out of the surface. This makes the lubrication condition inside the die orifice inefficient. For the solid lubricant film, there is no observable improvement in the pin height as well as the forming load. This is attributable to the film on the die surface tearing off during the process. Thus, at the end of the process, where the main pin forming occurs, there is no lubricant film to transfer the load, and consequently, no lubricant effect could be detected.

2. This unique behavior of insufficient liquid lubrication was investigated using the developed upper bound model. It was proven that the friction factor must be considered locally rather than a global friction factor, to get a reasonable validation for the model. The lubrication behavior on the die surface was assumed as normal, while considering the dry lubrication condition for inside the die orifice. The comparison of the experimental and modeling results proved the reliability of the localized-friction hypothesis.

3. It was theoretically shown that a successful lubrication inside the die orifice could increase the final part dimension significantly. The only suggestion would be using the solid coatings. However, the challenge in coating inside the die orifice with a diameter in the micro-scale is still there.

Chapter 10: Recommendations for Future Work

The work presented in this study has shed light on material behavior in a novel micro-forming process, which could be indicated as one more step towards mass production of micro-metallic parts. Moreover, it could motivate the further studies on the process improvements for various applications, and developments on the analytical modeling. Details are listed as below:

10.1. Electrical-assisted micro-forming

Hard-to-deform metals such as Titanium alloys or Stainless steels are having a vast application in medical industries. With an increasing demand for miniaturized components in these industries, micro-forming processes that improve the formability and reduce energy costs are continuously being investigated [31, 45, 199, 200]. One alternative could be using the elevated temperature micro-forming processes [42, 99]. This, however, results in a poor surface finish, reduced tool life, and reduced effectiveness of lubricants.

Electrical-assisted micro-forming can be used as another alternative, which involves passing a high current density through the specimen during micro-

forming [201, 202]. It is reported that when a high current electric density passes through a deforming sample, the electrons are scattered mainly at flaws such as impurities, dislocations and voids [203, 204]. This induces an increase in the rate of diffusion and energy state around the impurity, allowing easier dislocation movement, and subsequently increases the metal formability [205, 206]. To do so, a high current electrical supplier is required being able to provide electrical current densities up to 250 A/mm^2 . Since the electrical current affects the dislocation behavior, it would be interesting to study the grain/subgrain size effects in such processes.

10.2. Manufacturing of heat-sinks via progressive micro-forming process

With blooming micro-electronic industries, micro-pin heat sinks have been introduced as an effective tool for dissipating high heat fluxes in small areas [207]. Relying on the openness nature of the progressive micro-forming process, it can be implemented for production of arrays of micro-pins (as a heat sink), instead of only one micro-pin per punching. This significantly increases the heat-sink production rate, with having a benefit on less material wastage. Knowing the location of the neutral plane during the process, the dimension of the micro-pins and the gap between each pin on the heat sink can be precisely predicted using the modified Upper Bound theory.

Optimization of the process design needs 3D-FE simulation. DEFORM-3D could be a great help in this case.

10.3. Plane-Strain micro-fin forming

The work presented in this thesis was based on axi-symmetric boundary condition. It would be interesting to develop the micro-forming process in Plane-Strain boundary condition. Doing so, the process application will be spread to fabrications of micro-blades and micro-fins, having applications in medical and electronic industries. This would effectively reduce the production cost and material wastage that are the main drawback of existing manufacturing technologies [207].

To have a good control over the process, the proposed Upper Bound theory could be modified in Plane-Strain boundary condition. Alternatively, Slip-line theory could be used.

10.4. Modeling of the DMZ behavior

In Chapter 4, it was shown that using optimum punch diameter would remove the existence of the DMZ layer at the pin surface. This behavior was justified using the location of the neutral plane, monitored via FE simulation and Upper Bound theorem. By modeling the volume and shape of the DMZ at the head part during the process, and couple it with the Upper Bound model

results, it could be possible to predict the formation of the DMZ at the pin surface, in different circumstances.

The effect of initial material properties (grain size) and punch to die diameter ratio, together with friction must be considered in DMZ modeling. The Upper Bound theory seems to have the capability of being used for this purpose.

List of Publications

Peer-reviewed journal papers

1. E. Ghassemali, M-J Tan, C.B. Wah, A.E.W. Jarfors, S.C.V Lim, “*Grain size and workpiece dimension effects on material flow in open-die progressive micro-forging/extrusion*”, Materials Science and Engineering: A 582, (2013), 379–388.
2. E. Ghassemali, M-J Tan, A.E.W. Jarfors, S.C.V Lim, “*Optimization of the Axi-symmetric Open-die Micro Forging/Extrusion Processes: An Upper Bound Approach*”, International Journal of Mechanical Sciences 71, (2013), pp. 58-67.
3. E. Ghassemali, M-J Tan, A.E.W. Jarfors, S.C.V Lim, “*Progressive Microforming Process: Towards the Mass Production of Micro-parts Using Sheet Metal*”, International Journal of Advanced Materials Technology 66, (2013), pp. 611-621.
4. E. Ghassemali, A.E.W. Jarfors, M-J Tan, S.C.V. Lim, “*On the Microstructure of Copper Micro-Pins Manufactured by a Novel Progressive Microforming Process*”, International Journal of Material Forming 6, (2013), pp. 65-74.
5. E. Ghassemali, A.E.W. Jarfors, M-J Tan, S.C.V. Lim, “*Dead-Zone Formation and Micro-pin Properties in Progressive Micro-Forming Process*”, Steel Research International 9, (2011), pp. 1014-1019.

6. E. Ghassemali, A.E.W. Jarfors, M.J. Tan, S.C.V. Lim, and M. Chew, *“Investigation of microstructure and hardness in microforming of pure copper pins”*, Key Engineering Materials 447-448, (2010), pp. 381-385.
7. E. Ghassemali, M-J Tan, S.C.V Lim, C.B. Wah, A.E.W. Jarfors, *“Experimental and simulation of friction effects in an open-die micro-forging/extrusion process”*, Accepted for publication in ASME Journal of Micro- and Nano-Manufacturing, Jan. 2014.
8. E. Ghassemali, M-J Tan, A.E.W. Jarfors, C.B. Wah, S.C.V Lim, *“Effect of Cold-Work on the Hall-Petch Breakdown in Micro-components”*, Submitted to the Journal of the Mechanics and Physics of Solids, August 2013.
9. E. Ghassemali, M-J Tan, A.E.W. Jarfors, S.C.V Lim, C.B. Wah, *“Friction Effects on the Material Behavior During Open-die Micro Forging/Extrusion Processes: An Upper Bound Approach”*, Submitted to International Journal of Mechanical Sciences, August 2013.

Conference Presentations

1. E. Ghassemali, M-J Tan, S.C.V. Lim, A.E.W. Jarfors, “Friction Factor in a Progressive Microforming Process”, Oral presentation in ICOMM2012, 12-14 March 2012, Northwestern University, Chicago, USA.
2. E. Ghassemali, A.E.W. Jarfors, M-J Tan, S.C.V. Lim, “Dead-Zone Formation and Micro-pin Properties in Progressive Micro-Forming Process”, Oral Presentation in ICTP2011 , 25-30 September 2011, Aachen-Germany.

“Selected as the most pioneering work in the field of microforming”
3. E. Ghassemali, A.E.W. Jarfors, M.J. Tan, S.C.V. Lim, and M. Chew, “Investigation of microstructure and hardness in microforming of pure copper pins”, Oral presentation in ICOPE.2010, 28-30 July 2010, Singapore.

References

- [1] F. Chinesta, E. Cueto, U. Engel, A. Rosochowski, S. Geißdörfer, L. Olejnik, Microforming and Nanomaterials Advances in Material Forming, in, Springer Paris, 2007, pp. 99-124.
- [2] M. Geiger, F. Vollertsen, R. Kals, Fundamentals on the Manufacturing of Sheet Metal Microparts, CIRP Annals - Manufacturing Technology, 45 (1996) 277-282.
- [3] C.-P. Jiang, C.-C. Chen, Grain Size Effect on the Springback Behavior of the Microtube in the Press Bending Process, Materials and Manufacturing Processes, 27 (2011) 512-518.
- [4] F. Vollertsen, Z. Hu, Tribological size effects in sheet metal forming measured by a strip drawing test, CIRP Annals - Manufacturing Technology, 55 (2006) 291-294.
- [5] K.J. Hemker, W.N. Sharpe Jr, Microscale characterization of mechanical properties, Annual Review of Materials Research, 37 (2007) 92-126.
- [6] Y. Qin, Micro-forming and miniature manufacturing systems -- development needs and perspectives, Journal of Materials Processing Technology, 177 (2006) 8-18.
- [7] J. Jeswiet, M. Geiger, U. Engel, M. Kleiner, M. Schikorra, J. Duflou, R. Neugebauer, P. Bariani, S. Bruschi, Metal forming progress since 2000, CIRP Journal of Manufacturing Science and Technology, 1 (2008) 2-17.
- [8] A.J. Sanchez-Salmeron, R. Lopez-Tarazon, R. Guzman-Diana, C. Ricolfe-Viala, Recent development in micro-handling systems for micro-manufacturing, Journal of Materials Processing Technology, 167 (2005) 499-507.
- [9] U. Engel, A. Rosochowski, S. Geißdörfer, L. Olejnik, Microforming and Nanomaterials, in, 2007, pp. 99-124.
- [10] K. Hirota, Fabrication of micro-billet by sheet extrusion, Journal of Materials Processing Technology, 191 (2007) 283-287.
- [11] J. Cao, N. Krishnan, Z. Wang, H. Lu, W.K. Liu, A. Swanson, Microforming: Experimental investigation of the extrusion process for

micropins and its numerical simulation using RKEM, *Journal of Manufacturing Science and Engineering, Transactions of the ASME*, 126 (2004) 642-652.

[12] S. Antonio J, Chapter 18 - Handling for Micro-Manufacturing, in: *Micromanufacturing Engineering and Technology*, William Andrew Publishing, Boston, 2010, pp. 298-314.

[13] A. Molotnikov, R. Lapovok, C.H.J. Davies, W. Cao, Y. Estrin, Size effect on the tensile strength of fine-grained copper, *Scripta Materialia*, 59 (2008) 1182-1185.

[14] S.A. Parasiz, B. Kinsey, N. Krishnan, C. Jian, L. Ming, Investigation of deformation size effects during microextrusion, *Journal of Manufacturing Science and Engineering*, 129 (2007) 690-697.

[15] N. Krishnan, J. Cao, K. Dohda, Study of the Size Effects on Friction Conditions in Microextrusion---Part I: Microextrusion Experiments and Analysis, *Journal of Manufacturing Science and Engineering*, 129 (2007) 669-676.

[16] F. Vollertsen, D. Biermann, H.N. Hansen, I.S. Jawahir, K. Kuzman, Size effects in manufacturing of metallic components, *CIRP Annals - Manufacturing Technology*, 58 (2009) 566-587.

[17] B. Avitzur, *Metal Forming: Processes and Analysis*, New York, 1968.

[18] M. Geiger, M. Kleiner, R. Eckstein, N. Tiesler, U. Engel, Microforming, *CIRP Annals - Manufacturing Technology*, 50 (2001) 445-462.

[19] J. Jeon, A.N. Bramley, A friction model for microforming, *International Journal of Advanced Manufacturing Technology*, 33 (2007) 125-129.

[20] P. Becker, H.J. Jeon, C.C. Chang, A.N. Bramley, A geometric approach to modelling friction in metal forming, *CIRP Annals - Manufacturing Technology*, 52 (2003) 209-212.

[21] W.L. Chan, M.W. Fu, J. Lu, Experimental and simulation study of deformation behavior in micro-compound extrusion process, *Materials & Design*, 32 (2011) 525-534.

[22] R. Luttge, Chapter 2 - Basic Technologies for Microsystems, in: *Microfabrication for Industrial Applications*, William Andrew Publishing, Boston, 2011, pp. 13-54.

- [23] Y. Qin, A. Brockett, J. Zhao, A. Razali, Y. Ma, C. Harrison, Micromanufacturing Engineering and Technology, in: Micromanufacturing Engineering and Technology, William Andrew Publishing, Boston, 2010, pp. 130-145.
- [24] R. Gil, J.A. Sánchez, N. Ortega, S. Plaza, B. Izquierdo, I. Pombo, High-aspect ratio micro-pin manufacturing using inverse slab electrical discharge milling (ISEDm) process, *Int J Adv Manuf Technol*, (2012) 1-11.
- [25] W.L. Chan, M.W. Fu, Meso-scaled progressive forming of bulk cylindrical and flanged parts using sheet metal, *Materials & Design*, 43 (2013) 249-257.
- [26] F. Vollertsen, Z. Hu, H.S. Niehoff, C. Theiler, State of the art in micro forming and investigations into micro deep drawing, *Journal of Materials Processing Technology*, 151 (2004) 70-79.
- [27] K. Fujimoto, M. Yang, M. Hotta, H. Koyama, S. Nakano, K. Morikawa, J. Cairney, Fabrication of dies in micro-scale for micro-sheet metal forming, *Journal of Materials Processing Technology*, 177 (2006) 639-643.
- [28] S. Geißdörfer, A. Rosochowski, L. Olejnik, U. Engel, M. Richert, Micro-extrusion of ultrafine grained copper, *Int J Mater Form*, 1 (2008) 455-458.
- [29] W.J. Kim, Y.K. Sa, Micro-extrusion of ECAP processed magnesium alloy for production of high strength magnesium micro-gears, *Scripta Materialia*, 54 (2006) 1391-1395.
- [30] A. Rosochowski, W. Presz, L. Olejnik, M. Richert, Micro-extrusion of ultra-fine grained aluminium, *Int J Adv Manuf Technol*, 33 (2007) 137-146.
- [31] J.-T. Gau, C. Principe, J. Wang, An experimental study on size effects on flow stress and formability of aluminum and brass for microforming, *Journal of Materials Processing Technology*, 184 (2007) 42-46.
- [32] J.-T. Gau, C. Principe, M. Yu, Springback behavior of brass in micro sheet forming, *Journal of Materials Processing Technology*, 191 (2007) 7-10.
- [33] T.A. Kals, R. Eckstein, Miniaturization in sheet metal working, *Journal of Materials Processing Technology*, 103 (2000) 95-101.
- [34] Y. Saotome, K. Yasuda, H. Kaga, Microdeep drawability of very thin sheet steels, *Journal of Materials Processing Technology*, 113 (2001) 641-647.

- [35] M. Geiger, S. Geißdörfer, U. Engel, Mesoscopic model: advanced simulation of microforming processes, *Production Engineering*, 1 (2007) 79-84.
- [36] H.-J. Kwon, Y.-P. Jeon, C.-G. Kang, Effect of progressive forming process and processing variables on the formability of aluminium bipolar plate with microchannel, *Int J Adv Manuf Technol*, 64 (2013) 681-694.
- [37] Y. Shen, H.-p. Yu, X.-h. Dong, B. Guo, D.-b. Shan, S. Li, Simulation and discussion on the decreasing flow stress scale effect, *Journal of Shanghai Jiaotong University (Science)*, 17 (2012) 306-311.
- [38] M.W. Fu, W.L. Chan, A review on the state-of-the-art microforming technologies, *Int J Adv Manuf Technol*, (2012) 1-27.
- [39] E. Arzt, Size effects in materials due to microstructural and dimensional constraints: a comparative review, *Acta Materialia*, 46 (1998) 5611-5626.
- [40] D.J. Benson, H.-H. Fu, M.A. Meyers, On the effect of grain size on yield stress: extension into nanocrystalline domain, *Materials Science and Engineering A*, 319-321 (2001) 854-861.
- [41] W.L. Chan, M.W. Fu, J. Lu, The size effect on micro deformation behaviour in micro-scale plastic deformation, *Materials & Design*, In Press, Accepted Manuscript (2010).
- [42] E. Emil, U. Engel, G. Ulf, Process characterization and material flow in microforming at elevated temperatures, in, Elsevier, Kidlington, ROYAUME-UNI, 2004, pp. 6.
- [43] N. Krishnan, Microforming: Experimental investigation of size effects in the extrusion of micropins, in, Northwestern University, United States -- Illinois, 2006, pp. 186.
- [44] J.F. Michel, P. Picart, Size effects on the constitutive behaviour for brass in sheet metal forming, *Journal of Materials Processing Technology*, 141 (2003) 439-446.
- [45] N.A. Sène, P. Balland, R. Arrieux, K. Bouabdallah, An Experimental Study of the Microformability of Very Thin Materials, *Experimental Mechanics*, (2012) 1-8.
- [46] H.-k. Lee, B.-b. Jung, Y.-d. Kim, W.-b. Hwang, H.-c. Park, Analysis of flow stress and size effect on polycrystalline metallic materials in tension, *Materials Science and Engineering: A*, 527 (2009) 339-343.

- [47] F. Vollertsen, H. Schulze Niehoff, Z. Hu, State of the art in micro forming, *International Journal of Machine Tools and Manufacture*, 46 (2006) 1172-1179.
- [48] K. Hanada, L. Zhang, M. Mayuzumi, T. Sano, Fabrication of diamond dies for microforming, *Diamond and Related Materials*, 12 757-761.
- [49] T. Connolley, P.E. McHugh, M. Bruzz, A review of deformation and fatigue of metals at small size scales, *Fatigue and Fracture of Engineering Material and Structures*, 28 (2005) 1119-1152.
- [50] S. Mahabunphachai, M. Koç, Investigation of size effects on material behavior of thin sheet metals using hydraulic bulge testing at micro/meso-scales, *International Journal of Machine Tools and Manufacture*, 48 (2008) 1014-1029.
- [51] F. Vollertsen, Categories of size effects, *Production Engineering*, 2 (2008) 377-383.
- [52] R.W. Armstrong, On size effects in polycrystal plasticity, *Journal of the Mechanics and Physics of Solids*, 9 (1961) 196-199.
- [53] S.E. Geissdoarfer, U. Geiger, M., An advanced approach in simulation of microforming processes, in: W.M.S. Dimov (Ed.) 4M 2005 - First International Conference on Multi-Material Micro Manufacture, Germany, 2005.
- [54] M.R. Begley, J.W. Hutchinson, The mechanics of size-dependent indentation, *Journal of the Mechanics and Physics of Solids*, 46 (1998) 2049-2068.
- [55] W. Zheng, G. Wang, G. Zhao, D. Wei, Z. Jiang, Modeling and analysis of dry friction in micro-forming of metals, *Tribology International*, 57 (2013) 202-209.
- [56] N. Fang, Slip-line modeling of machining with a rounded-edge tool—Part I: new model and theory, *Journal of the Mechanics and Physics of Solids*, 51 (2003) 715-742.
- [57] F.-H. Yeh, C.-L. Li, Y.-H. Lu, Study of thickness and grain size effects on material behavior in micro-forming, *Journal of Materials Processing Technology*, 201 (2008) 237-241.
- [58] I. Manika, J. Maniks, Size effects in micro- and nanoscale indentation, *Acta Materialia*, 54 (2006) 2049-2056.

- [59] W. Chan, M. Fu, Studies of the interactive effect of specimen and grain sizes on the plastic deformation behavior in microforming, *Int J Adv Manuf Technol*, 62 (2012) 989-1000.
- [60] S. Lefebvre, B. Devincere, T. Hoc, Simulation of the Hall-Petch effect in ultra-fine grained copper, *Materials Science and Engineering A*, 400-401 (2005) 150-153.
- [61] S. Geißdörfer, U. Engel, M. Geiger, FE-simulation of microforming processes applying a mesoscopic model, *International Journal of Machine Tools and Manufacture*, 46 (2006) 1222-1226.
- [62] S. Miyazaki, K. Shibata, H. Fujita, Effect of specimen thickness on mechanical properties of polycrystalline aggregates with various grain sizes, *Acta Metallurgica*, 27 (1979) 855-862.
- [63] L.V. Raulea, A.M. Goijaerts, L.E. Govaert, F.P.T. Baaijens, Size effects in the processing of thin metal sheets, *Journal of Materials Processing Technology*, 115 (2001) 44-48.
- [64] J.S. Stölken, A.G. Evans, A microbend test method for measuring the plasticity length scale, *Acta Materialia*, 46 (1998) 5109-5115.
- [65] N.A. Fleck, G.M. Muller, M.F. Ashby, J.W. Hutchinson, Strain gradient plasticity: Theory and experiment, *Acta Metallurgica et Materialia*, 42 (1994) 475-487.
- [66] J.J. Gracio, The effect of grain size on the microstructural evolution of copper deformed in rolling, *Materials Science and Engineering: A*, 196 (1995) 97-104.
- [67] H. Justinger, G. Hirt, Estimation of grain size and grain orientation influence in microforming processes by Taylor factor considerations, *Journal of Materials Processing Technology*, 209 (2009) 2111-2121.
- [68] W.D. Nix, G. Huajian, Indentation size effects in crystalline materials: a law for strain gradient plasticity, *Journal of the Mechanics and Physics of Solids*, 46 (1998) 411-425.
- [69] B. Wilshire, C.J. Palmer, Grain size effects during creep of copper, *Scripta Materialia*, 46 (2002) 483-488.
- [70] G.-Y. Kim, J. Ni, M. Koc, Modeling of the Size Effects on the Behavior of Metals in Microscale Deformation Processes, *Journal of Manufacturing Science and Engineering*, 129 (2007) 470-476.

- [71] L. Daw-Kwei, Modeling of size effect on tensile flow stress of sheet metal in microforming, *Journal of Manufacturing Science and Engineering*, 131 (2009) 011002 (011008 pp.).
- [72] L. Peng, F. Liu, J. Ni, X. Lai, Size effects in thin sheet metal forming and its elastic-plastic constitutive model, *Materials & Design*, 28 (2007) 1731-1736.
- [73] D.-b. Shan, C.-j. Wang, B. Guo, X.-w. Wang, Effect of thickness and grain size on material behavior in micro-bending, *Transactions of Nonferrous Metals Society of China*, 19 (2009) s507-s510.
- [74] S.A. Parasız, B.L. Kinsey, N. Mahayatsanun, J. Cao, Effect of specimen size and grain size on deformation in microextrusion, *Journal of Manufacturing Processes*, 13 (2011) 153-159.
- [75] C. Keller, E. Hug, X. Feaugas, Microstructural size effects on mechanical properties of high purity nickel, *International Journal of Plasticity*, 27 (2011) 635-654.
- [76] A. Messner, U. Engel, R. Kals, F. Vollertsen, Size effect in the FE-simulation of micro-forming processes, *Journal of Materials Processing Technology*, 45 (1994) 371-376.
- [77] A. Molotnikov, R. Lapovok, C.F. Gu, C.H.J. Davies, Y. Estrin, Size effects in micro cup drawing, *Materials Science and Engineering: A*, 550 (2012) 312-319.
- [78] J.E. Flinn, D.P. Field, G.E. Korth, T.M. Lillo, J. Macheret, The flow stress behavior of OFHC polycrystalline copper, *Acta Materialia*, 49 (2001) 2065-2074.
- [79] Y. Shen, H.-p. Yu, X.-y. Ruan, Discussion and prediction on decreasing flow stress scale effect, *Transactions of Nonferrous Metals Society of China*, 16 (2006) 132-136.
- [80] C.-j. Wang, B. Guo, D.-b. Shan, L.-n. Sun, Effects of specimen size on flow stress of micro rod specimen, *Transactions of Nonferrous Metals Society of China*, 19 (2009) s511-s515.
- [81] M. Klein, A. Hadrboletz, B. Weiss, G. Khatibi, The '[]size effect' on the stress-strain, fatigue and fracture properties of thin metallic foils, *Materials Science and Engineering A*, 319-321 (2001) 924-928.

- [82] J.Q. Ran, M.W. Fu, W.L. Chan, The influence of size effect on the ductile fracture in micro-scaled plastic deformation, *International Journal of Plasticity*, 41 (2013) 65-81.
- [83] C. Keller, E. Hug, R. Retoux, X. Feaugas, TEM study of dislocation patterns in near-surface and core regions of deformed nickel polycrystals with few grains across the cross section, *Mechanics of Materials*, 42 (2010) 44-54.
- [84] E. Hug, C. Keller, Intrinsic Effects due to the Reduction of Thickness on the Mechanical Behavior of Nickel Polycrystals, *Metallurgical and Materials Transactions A*, 41 (2010) 2498-2506.
- [85] J.J. Gracio, J.V. Fernandes, J.H. Schmitt, Effect of grain size on substructural evolution and plastic behaviour of copper, *Materials Science and Engineering: A*, 118 (1989) 97-105.
- [86] A.A.W. Thompson, Yielding in nickel as a function of grain or cell size, *Acta Metallurgica*, 23 (1975) 1337-1342.
- [87] F. Liu, L.F. Peng, X.M. Lai, Study on the size effect and the effect of the friction coefficient on the micro-extrusion process, in: *Robotic Welding, Intelligence and Automation. (Lecture Notes in Control and Information Sciences vol. 362)*, Springer-Verlag, Berlin, Germany, 2007, pp. 493-499.
- [88] T.L. Johnston, C.E. Feltner, Grain size effects in the strain hardening of polycrystals, *Metall and Materi Trans*, 1 (1970) 1161-1167.
- [89] A.W. Thompson, M.I. Baskes, W.F. Flanagan, The dependence of polycrystal work hardening on grain size, *Acta Metallurgica*, 21 (1973) 1017-1028.
- [90] S.H. Chen, T.C. Wang, A new hardening law for strain gradient plasticity, *Acta Materialia*, 48 (2000) 3997-4005.
- [91] J.W. Hutchinson, Plasticity at the micron scale, *International Journal of Solids and Structures*, 37 (2000) 225-238.
- [92] U.F. Kocks, H. Mecking, Physics and phenomenology of strain hardening: the FCC case, *Progress in Materials Science*, 48 (2003) 171-273.
- [93] H. Mughrabi, On the current understanding of strain gradient plasticity, *Materials Science and Engineering A*, 387-389 (2004) 209-213.
- [94] H. Gao, Y. Huang, Geometrically necessary dislocation and size-dependent plasticity, *Scripta Materialia*, 48 (2003) 113-118.

- [95] S.A. Parasiz, B.L. Kinsey, Prediction of Strain Gradient Hardening During Microextrusion, *Materials and Manufacturing Processes*, 25 (2010) 769-776.
- [96] M. Henning, H. Vehoff, Statistical size effects based on grain size and texture in thin sheets, *Materials Science and Engineering: A*, 452-453 (2007) 602-613.
- [97] E. Egerer, U. Engel, MATERIAL BEHAVIOR IN MICROFORMING at elevated temperature, in: *ASPE 2003 Winter Topical Meeting 2003*, pp. 37-42.
- [98] J.P. Wulfsberg, M. Terzi, Investigation of laser heating in microforming applying sapphire tools, *CIRP Annals - Manufacturing Technology*, 56 (2007) 321-326.
- [99] B. Eichenhüller, U. Engel, Microforming of titanium – forming behaviour at elevated temperature, *Proceedings of the Institution of Mechanical Engineers, Part B: Journal of Engineering Manufacture*, 222 (2008) 77-82.
- [100] H. Tanabe, M. Yang, Design and evaluation of heat assisted microforming system, *Steel Research International, Special Ed. ICTP2011* (2011) 1020-1024.
- [101] A. Fiorentino, E. Ceretti, C. Giardini, Tube hydroforming compression test for friction estimation—numerical inverse method, application, and analysis, *Int J Adv Manuf Technol*, 64 (2013) 695-705.
- [102] M. Taureza, S. Castagne, Y. Aue-u-lan, S.C.V. Lim, The influence of die geometry and workpiece mechanical properties in T-Shape friction test, *Journal of Materials Processing Technology*, 212 (2012) 2413-2423.
- [103] L. Peng, X. Lai, H.-J. Lee, J.-H. Song, J. Ni, Friction behavior modeling and analysis in micro/meso scale metal forming process, *Materials & Design*, 31 (2010) 1953-1961.
- [104] J.H. Deng, M.W. Fu, W.L. Chan, Size effect on material surface deformation behavior in micro-forming process, *Materials Science and Engineering: A*, 528 (2011) 4799-4806.
- [105] N. Tiesler, U. Engel, M. Geiger, Forming of Microparts - Effects of miniaturization on friction, in: *6th ICTP 1999, Germany, 1999*, pp. 889-894.
- [106] M.I. Ghobrial, J.Y. Lee, T. Altan, N. Bay, B.G. Hansen, Factors affecting the double cup extrusion test for evaluation of friction in cold and warm forging, *CIRP Annals - Manufacturing Technology*, 42 (1993) 347-351.

- [107] U. Engel, Tribology in microforming, *Wear*, 260 (2006) 265-273.
- [108] A. Forcellese, F. Gabrielli, A. Barcellona, F. Micari, Evaluation of friction in cold metal forming, *Journal of Materials Processing Technology*, 45 (1994) 619-624.
- [109] L.F. Mori, N. Krishnan, J. Cao, H.D. Espinosa, Study of the Size Effects and Friction Conditions in Microextrusion---Part II: Size Effect in Dynamic Friction for Brass-Steel Pairs, *Journal of Manufacturing Science and Engineering*, 129 (2007) 677-689.
- [110] T. Nellesmann, N. Bay, T. Wanheim, Real area of contact and friction stress — The role of trapped lubricant, *Wear*, 43 (1977) 45-53.
- [111] L. Daw-Kwei, A simple dry friction model for metal forming process, *Journal of Materials Processing Technology*, 209 (2009) 2361-2368.
- [112] S. Weidel, U. Engel, Characterisation of the flattening behaviour of modelled asperities, *Wear*, 266 (2009) 596-599.
- [113] N.W. Bay, T., Tribology in Metal Forming, in: *International Conference on Technology of Plasticity, Japan, 2002*, pp. 309-320.
- [114] R. Eriksen, S. Weidel, H. Hansen, Tribological influence of tool surface roughness within microforming, *Int J Mater Form*, 3 (2010) 419-422.
- [115] J. Bech, N. Bay, M. Eriksen, A Study of Mechanisms of Liquid Lubrication in Metal Forming, *CIRP Annals - Manufacturing Technology*, 47 (1998) 221-226.
- [116] C.G. Sørensen, J.I. Bech, J.L. Andreasen, N. Bay, U. Engel, T. Neudecker, A Basic Study of the Influence of Surface Topography on Mechanisms of Liquid Lubrication in Metal Forming, *CIRP Annals - Manufacturing Technology*, 48 (1999) 203-208.
- [117] N. Bay, T. Wanheim, Real area of contact and friction stress at high pressure sliding contact, *Wear*, 38 (1976) 201-209.
- [118] S.C. Lim, M.F. Ashby, J.H. Brunton, The effects of sliding conditions on the dry friction of metals, *Acta Metallurgica*, 37 (1989) 767-772.
- [119] S.W. Baek, S.I. Oh, S.H. Rhim, Lubrication for Micro Forming of Ultra Thin Metal Foil, *CIRP Annals - Manufacturing Technology*, 55 (2006) 295-298.

- [120] B. Guo, F. Gong, C. Wang, D. Shan, Size effect on friction in scaled down strip drawing, *Journal of Materials Science*, 45 (2010) 4067-4072.
- [121] G. Ngaile, C. Bunget, Influence of ultrasonic vibration on microforming, in: *Transactions of the North American Manufacturing Research Institution of SME 2008*. NAMRC 36, 20-23 May 2008, Society of Manufacturing Engineers, Dearborn, MI, USA, 2008, pp. 137-144.
- [122] C. Bunget, G. Ngaile, Influence of ultrasonic vibration on micro-extrusion, *Ultrasonics*, 51 (2011) 606-616.
- [123] Y. Seo, C. Park, B. Kim, H. Lee, N. Lee, Development of audio frequency vibration microforming system, *International Journal of Precision Engineering and Manufacturing*, 13 (2012) 789-794.
- [124] B. Eichenhuller, U. Engel, S. Geidorfer, Process parameter interaction in microforming, *Int J Mater Form*, 1 (2008) 451-454.
- [125] Y. Okazaki, N. Mishima, K. Ashida, Microfactory---Concept, History, and Developments, *Journal of Manufacturing Science and Engineering*, 126 (2004) 837-844.
- [126] U. Engel, R. Eckstein, Microforming--from basic research to its realization, *Journal of Materials Processing Technology*, 125-126 (2002) 35-44.
- [127] M. Tanaka, Development of desktop machining microfactory, *RIKEN Review*, 34 (2001) 46-49.
- [128] W. Chunju, S. Debin, G. Bin, Z. Jian, S. Lining, Key problems in microforming processes of microparts, *Journal of Materials Science & Technology*, 23 (2007) 283-288.
- [129] G.C. Wang, W. Zheng, T. Wu, H. Jiang, G.Q. Zhao, D.B. Wei, Z.Y. Jiang, A multi-region model for numerical simulation of micro bulk forming, *Journal of Materials Processing Technology*, 212 (2012) 678-684.
- [130] C.J. Wang, D.B. Shan, J. Zhou, B. Guo, L.N. Sun, Size effects of the cavity dimension on the microforming ability during coining process, *Journal of Materials Processing Technology*, 187-188 (2007) 256-259.
- [131] H. Zhang, B.E. Schuster, Q. Wei, K.T. Ramesh, The design of accurate micro-compression experiments, *Scripta Materialia*, 54 (2006) 181-186.

- [132] F.-K. Chen, J.-W. Tsai, A study of size effect in micro-forming with micro-hardness tests, *Journal of Materials Processing Technology*, 177 (2006) 146-149.
- [133] A. Razali, Y. Qin, J. Zhao, C. Harrison, R. Smith, Development of a New High-Precision Feeder for Micro-Sheet-Forming, *Journal of Manufacturing Science and Engineering*, 133 (2011) 061025-061027.
- [134] M. Arentoft, R.S. Eriksen, H.N.Hansen, N.A. Paldan, Towards the first generation micro bulk forming system, *CIRP Annals - Manufacturing Technology*, 60 (2011) 335-338.
- [135] U. Engel, A. Rosochowski, S. Geißdörfer, L. Olejnik, Microforming and Nanomaterials, in: *Advances in Material Forming*, Springer Paris, 2007, pp. 99-124.
- [136] M. Rosochowska, A. Rosochowski, L. Olejnik, FE simulation of micro-extrusion of a conical pin, *Int J Mater Form*, 3 (2010) 423-426.
- [137] E. Galvis, B.A. Jubran, F.X.K. Behdinan, Z. Fawaz, Numerical modeling of pin-fin micro heat exchangers, *Heat and Mass Transfer*, 44 (2008) 659-666.
- [138] P.M. Dixit, U.S. Dixit, Optimization of Metal Forming and Machining Processes; Modeling of Metal Forming and Machining Processes, (2008) 549-577.
- [139] M. Dunand, A.P. Maertens, M. Luo, D. Mohr, Experiments and modeling of anisotropic aluminum extrusions under multi-axial loading – Part I: Plasticity, *International Journal of Plasticity*, 36 (2012) 34-49.
- [140] N. Venkata Reddy, P.M. Dixit, G.K. Lal, Die design for axisymmetric hot extrusion, *International Journal of Machine Tools and Manufacture*, 37 (1997) 1635-1650.
- [141] J. Ajiboye, Upper bound analysis of extrusion from square billets through circular and square/rectangular dies, *Journal of Mechanical Science and Technology*, 23 (2009) 461-474.
- [142] C.H. Lee, T. Altan, Influence of Flow Stress and Friction Upon Metal Flow in Upset Forging of Rings and Cylinders, *Journal of Engineering for Industry*, 94 (1972) 775-782.
- [143] B. Avitzur, C.J.V. Tyne, S. Turczyn, The Prevention of Central Bursts During Rolling, *Journal of Engineering for Industry*, 110 (1988) 173-178.

- [144] Y.H. Moon, C.J. Van Tyne, W.A. Gordon, An upper bound analysis of a process-induced side-surface defect in forgings: Part 1: The velocity fields and power terms, *Journal of Materials Processing Technology*, 99 (2000) 169-178.
- [145] B. Avitzur, C.J.V. Tyne, Ring Forming: An Upper Bound Approach. Part 1: Flow Pattern and Calculation of Power, *Journal of Engineering for Industry*, 104 (1982) 231-237.
- [146] W.-C. Yeh, M.-C. Wu, A variational upper-bound method for analysis of upset forging of rings, *Journal of Materials Processing Technology*, 170 (2005) 392-402.
- [147] H. Kudo, An upper-bound approach to plane-strain forging and extrusion—I, *International Journal of Mechanical Sciences*, 1 (1960) 57-83.
- [148] J.S. Ajiboye, M.B. Adeyemi, Upper bound analysis of die land length in cold extrusion, *Journal of Materials Processing Technology*, 177 (2006) 608-611.
- [149] J.S. Ajiboye, M.B. Adeyemi, Upper bound analysis for extrusion at various die land lengths and shaped profiles, *International Journal of Mechanical Sciences*, 49 (2007) 335-351.
- [150] A. Agrawal, N.V. Reddy, P.M. Dixit, Optimal blank shape prediction considering sheet thickness variation: An upper bound approach, *Journal of Materials Processing Technology*, 196 (2008) 249-258.
- [151] R. Sowerby, J.L. Duncan, E. Chu, The modelling of sheet metal stampings, *International Journal of Mechanical Sciences*, 28 (1986) 415-430.
- [152] B.C. Hwang, S.J. Hong, W.B. Bae, An UBET analysis of the non-axisymmetric extrusion/forging process, *Journal of Materials Processing Technology*, 111 (2001) 135-141.
- [153] C.-Y. Wu, Y.-C. Hsu, The influence of die shape on the flow deformation of extrusion forging, *Journal of Materials Processing Technology*, 124 (2002) 67-76.
- [154] J. Vickery, J. Monaghan, An investigation of the early stages of a forging/extrusion process, *Journal of Materials Processing Technology*, 43 (1994) 37-50.
- [155] B. Avitzur, F. Sauerwine, Limit Analysis of Hollow Disk Forging---Part 1: Upper Bound, *Journal of Engineering for Industry*, 100 (1978) 340-344.

- [156] L. Brayden, J. Monaghan, An analysis of closed-die extrusion/forging, *Journal of Materials Processing Technology*, 26 (1991) 141-157.
- [157] Y.T. Lin, J.P. Wang, A new upper-bound elemental technique approach to axisymmetric metal forming processes, *International Journal of Machine Tools and Manufacture*, 33 (1993) 135-151.
- [158] A. Danno, W. C.C, L. S.C.V, Capability Development of Progressive Extrusion Forming of Hollow Miniature Micro Pin from Sheet Material, in: *International Forum on Micro Manufacturing (IFMM2010)*, Japan, 2010, pp. 241-244.
- [159] K. Osakada, K. Mori, T. Altan, P. Groche, Mechanical servo press technology for metal forming, *CIRP Annals - Manufacturing Technology*, 60 (2011) 651-672.
- [160] Y. Sun, J. Hu, Y. Cheng, W. Ruan, Characteristics analysis of working mechanisms for servo mechanical presses, in: *2nd International Conference on Advanced Design and Manufacturing Engineering, ADME 2012*, August 16, 2012 - August 18, 2012, Trans Tech Publications, Taiyuan, China, 2012, pp. 762-767.
- [161] A.H. Shabaik, Prediction of the Geometry Changes of the Free Boundary During Upsetting by the Slip-Line Theory, *Journal of Engineering for Industry*, 93 (1971) 586-592.
- [162] A. Watanabe, H. Watari, T. Yamazaki, Basic study on dynamic characteristics for servo-press machine, in: *SICE 2012 - 51st Annual Conference of the Society of Instrument and Control Engineers of Japan*, 20-23 Aug. 2012, IEEE, Piscataway, NJ, USA, 2012, pp. 1477-1480.
- [163] D.R. Hayhurst, M.W. Chan, Determination of friction models for metallic die-workpiece interfaces, *International Journal of Mechanical Sciences*, 47 (2005) 1-25.
- [164] F.J. Humphreys, M. Hatherly, Chapter 6 - Recovery After Deformation, in: *Recrystallization and Related Annealing Phenomena (Second Edition)*, Elsevier, Oxford, 2004, pp. 169-213.
- [165] M.J. Luton, R.A. Petkovic, J.J. Jonas, Kinetics of recovery and recrystallization in polycrystalline copper, *Acta Metallurgica*, 28 (1980) 729-743.

- [166] F.J. Humphreys, M. Hatherly, Chapter 5 - The Mobility and Migration of Boundaries, in: *Recrystallization and Related Annealing Phenomena* (Second Edition), Elsevier, Oxford, 2004, pp. 121-167.
- [167] G. Van Drunen, S. Saimoto, Deformation and recovery of [001] oriented copper crystals, *Acta Metallurgica*, 19 (1971) 213-221.
- [168] ISO, Geometrical Product Specifications (GPS) -- Surface texture: Profile method -- Rules and procedures for the assessment of surface texture, in: *Dimensional and geometrical product specifications and verification*, 1996.
- [169] G. Wu, D. Juul Jensen, Automatic determination of recrystallization parameters based on EBSD mapping, *Materials Characterization*, 59 (2008) 794-800.
- [170] F.J. Humphreys, Review Grain and subgrain characterisation by electron backscatter diffraction, *Journal of Materials Science*, 36 (2001) 3833-3854.
- [171] W.Q. Cao, A. Godfrey, Q. Liu, Determining dislocation cell sizes for high-strain deformation microstructures using the EBSD technique, *Journal of Microscopy*, 211 (2003) 219-229.
- [172] F. Salimyanfard, M. Reza Toroghinejad, F. Ashrafizadeh, M. Jafari, EBSD analysis of nano-structured copper processed by ECAP, *Materials Science and Engineering: A*, 528 (2011) 5348-5355.
- [173] P.S. Bate, R.D. Knutsen, I. Brough, F.J. Humphreys, The characterization of low-angle boundaries by EBSD, *Journal of Microscopy*, 220 (2005) 36-46.
- [174] W. Mason, P.F. Johnson, J.R. Varner, Importance of load cell sensitivity in determination of the load dependence of hardness in recording microhardness tests, *Journal of Materials Science*, 26 (1991) 6576-6580.
- [175] S. Buzzi, M. Dietiker, K. Kunze, R. Spolenak, J.F. Löffler, Deformation behavior of silver submicrometer-pillars prepared by nanoimprinting, *Philosophical Magazine*, 89 (2009) 869-884.
- [176] P. Robinson, Properties of Wrought Coppers and Copper Alloys, in: *ASM Handbook*, ASM International, 1990, pp. 265-345.
- [177] M.N. Huang, G.Y. Tzou, Study on compression forming of a rotating disk considering hybrid friction, *Journal of Materials Processing Technology*, 125-126 (2002) 421-426.

- [178] J. Cao, W. Zhuang, S. Wang, J. Lin, Development of a VGRAIN system for CPFE analysis in micro-forming applications, *Int J Adv Manuf Technol*, 47 (2010) 981-991.
- [179] A. Buschhausen, K. Weinmann, J.Y. Lee, T. Altan, Evaluation of lubrication and friction in cold forging using a double backward-extrusion process, *Journal of Materials Processing Technology*, 33 (1992) 95-108.
- [180] Y. Hou, W. Zhang, Z. Yu, S. Li, Selection of tool materials and surface treatments for improved galling performance in sheet metal forming, *Int J Adv Manuf Technol*, 43 (2009) 1010-1017.
- [181] T. Altan, S.-I. Oh, H.L. Gegel, Metal Forming: Fundamentals and Applications, in, American Society for Metals, 1983, pp. 156-158.
- [182] S. Kobayashi, Theories and Experiments on Friction, Deformation, and Fracture in Plastic Deformation Processes, in: A.L. Hoffmann (Ed.) *Metal Forming: Interrelation Between Theory and Practice*, Springer US, 1971, pp. 325-347.
- [183] P. Dadras, J.F. Thomas Jr, Analysis of axisymmetric upsetting based on flow pattern observations, *International Journal of Mechanical Sciences*, 25 (1983) 421-427.
- [184] J.A. Schey, *Tribology in Metalworking*, American Society for Metals, 1984.
- [185] E.O. Hall, The Deformation and Ageing of Mild Steel: III Discussion of Results, *Proceedings of the Physical Society. Section B*, 64 (1951) 747.
- [186] N.J. Petch, The cleavage strength of polycrystals, *Journal of the Iron and Steel Institute*, 174 (1953) 25-28.
- [187] D.R. Lesuer, C.K. Syn, O.D. Sherby, Nano-subgrain strengthening in ball-milled iron, *Materials Science and Engineering: A*, 463 (2007) 54-60.
- [188] L.P. Evers, D.M. Parks, W.A.M. Brekelmans, M.G.D. Geers, Crystal plasticity model with enhanced hardening by geometrically necessary dislocation accumulation, *Journal of the Mechanics and Physics of Solids*, 50 (2002) 2403-2424.
- [189] H.H. Fu, D.J. Benson, M.A. Meyers, Analytical and computational description of effect of grain size on yield stress of metals, *Acta Materialia*, 49 (2001) 2567-2582.

- [190] S.Z. Qamar, Shape Complexity, Metal Flow, and Dead Metal Zone in Cold Extrusion, *Materials and Manufacturing Processes*, 25 (2010) 1454-1461.
- [191] N.M. Cordero, S. Forest, E.P. Busso, S. Berbenni, M. Cherkaoui, Grain size effects on plastic strain and dislocation density tensor fields in metal polycrystals, *Computational Materials Science*, 52 (2012) 7-13.
- [192] P.I. Dolgosheev, V.E. Sytnikov, G.G. Svalov, N.V. Polyakova, D.I. Belij, A.B. Lopes, S.B. Petersen, J.M.C. Rodrigues, P.A.F. Martins, J.J. Gracio, Injection forging of tubes: results of macroscopic analysis and comparison with microstructural observations, *Materials Science and Engineering: A*, 248 (1998) 276-286.
- [193] C. Lowe Terry, Z. Valliev Ruslan, *Investigations and Applications of Severe Plastic Deformation*, Russia, 1999.
- [194] A. Güzel, A. Jäger, F. Parvizian, H.G. Lambers, A.E. Tekkaya, B. Svendsen, H.J. Maier, A new method for determining dynamic grain structure evolution during hot aluminum extrusion, *Journal of Materials Processing Technology*, 212 (2012) 323-330.
- [195] M.R. Staker, D.L. Holt, The dislocation cell size and dislocation density in copper deformed at temperatures between 25 and 700°C, *Acta Metallurgica*, 20 (1972) 569-579.
- [196] F.J. Humphreys, M. Hatherly, Chapter 10 - The Growth and Stability of Cellular Microstructures, in: *Recrystallization and Related Annealing Phenomena* (Second Edition), Elsevier, Oxford, 2004, pp. 321-331.
- [197] J. William D. Callister, D.G. Rethwisch, *Materials Science and Engineering: An Introduction*, John Wiley & Sons Canada, Limited, 2010.
- [198] W.L. Chan, M.W. Fu, Experimental studies of plastic deformation behaviors in microheading process, *Journal of Materials Processing Technology*, 212 (2012) 1501-1512.
- [199] S.G. Kang, Y.S. Na, K.Y. Park, J.E. Jeon, S.C. Son, J.H. Lee, A study on the micro-formability of Al 5083 superplastic alloy using micro-forging method, *Materials Science and Engineering: A*, 449-451 (2007) 338-342.
- [200] Y. Saotome, H. Iwazaki, Superplastic backward microextrusion of microparts for micro-electro-mechanical systems, *Journal of Materials Processing Technology*, 119 (2001) 307-311.

- [201] J.J. Jones, L. Mears, J.T. Roth, Electrically-Assisted Forming of Magnesium AZ31: Effect of Current Magnitude and Deformation Rate on Forgeability Journal of Manufacturing Science and Engineering, 134 (2012) 7.
- [202] W.A. Salandro, C.J. Bunget, L. Mears, Several Factors Affecting the Electroplastic Effect During an Electrically-Assisted Forming Process Journal of Manufacturing Science and Engineering, 133 (2011) 5.
- [203] T.A. Perkins, T.J. Kronenberger, J.T. Roth, Metallic Forging Using Electrical Flow as an Alternative to Warm/Hot Working, J Manuf Sci Eng, 129 (2007) 84-94.
- [204] H. Conrad, Electroplasticity in metals and ceramics, Materials Science and Engineering: A, 287 (2000) 276-287.
- [205] J. Mai, L. Peng, X. Lai, Z. Lin, Electrical-assisted embossing process for fabrication of micro-channels on 316L stainless steel plate, Journal of Materials Processing Technology, 213 (2013) 314-321.
- [206] M.S. Siopis, B.L. Kinsey, Experimental Investigation of Grain and Specimen Size Effects During Electrical-Assisted Forming Journal of Manufacturing Science and Engineering, 132 (2010) 7.
- [207] B.A. Jasperson, J. Yongho, K.T. Turner, F.E. Pfefferkorn, Q. Weilin, Comparison of Micro-Pin-Fin and Microchannel Heat Sinks Considering Thermal-Hydraulic Performance and Manufacturability, Components and Packaging Technologies, IEEE Transactions on, 33 (2010) 148-160.

Appendix

A-1- Expanded equations

The location of the neutral plane moves during the process to minimize the required forming energy. Thus, the below procedure can be followed to find the relative location of the neutral plane during the process (Eq. A.1).

$$\frac{\partial J^*}{\partial R_n} = 0$$

$$\Leftrightarrow \left\{ \begin{aligned} & \frac{4R_n^2}{R_0^2} \left(\left(2f(\alpha) + \frac{2mh(\alpha)}{\sqrt{3}} \right) \ln \left(\frac{R_n}{R_i} \right) + \frac{2g(\alpha)}{\sqrt{3}} + 2m \frac{R_n}{R_i} \right) \\ & + \frac{R_n^3}{R_0^2} \left(2 \left(F(\alpha) \ln \left(\frac{R_n}{R_i} \right) + \frac{f(\alpha)}{R_i} \right) + \frac{2G(\alpha)}{\sqrt{3}} \right. \\ & \quad \left. + \frac{3m}{\sqrt{3}} \left(H(\alpha) \ln \left(\frac{R_n}{R_i} \right) + \frac{h(\alpha)}{R_i} \right) + \frac{2m}{\sqrt{3}R_i} \right) \\ & - \frac{\left(2 - 4 \left(\frac{R_n}{R_i} \right)^2 \right)}{\sqrt{3}} \left(a(R_n) + 2 + \ln(b(R_n)) - \frac{2mR_n}{T} c(R_n) \right) \\ & - \frac{\left(R_n - \frac{R_n^3}{R_0^2} \right)}{\sqrt{3}} \left(A(R_n) + \frac{B(R_n)}{b(R_n)} - \frac{2m}{T} (c(R_n) + R_n C(R_n)) \right) \end{aligned} \right\} = 0$$

(A.1)

here

$$A(R_n) = \frac{\partial a(R_n)}{\partial R_n}, \quad B(\alpha) = \frac{\partial b(R_n)}{\partial R_n}, \quad F(\alpha) = \frac{\partial f(\alpha)}{\partial R_n}, \quad H(\alpha) = \frac{\partial h(\alpha)}{\partial R_n}$$

(A.2)

From this equation, the value of the critical thickness can be achieved as Eq.

(A.3).

$$\frac{R_0}{T} = \frac{\sqrt{3} \frac{R_0}{R_n}}{2m \left\{ \left(\left(\frac{R_n}{R_0} \right)^2 - 1 \right) \left(6 \frac{R_0}{R_n} - 6 \right) - 2 \left(\frac{R_n}{R_0} \right)^2 c(R_n) \right\}} \times$$

$$\left[\begin{aligned} & 2\sqrt{3} \left(\left(\frac{R_n}{R_0} \right)^2 - 1 \right) (2 + J(R_n) - a(R_n)) \\ & \left[\begin{aligned} & \frac{0.15}{\alpha} + \frac{2(-\cos(\alpha)K(R_n) + L(R_n))}{\sin^2(\alpha)} \\ & + \frac{2\ln\left(\frac{R_n}{R_i}\right)}{\sin^2(\alpha)} \left(\left(\frac{0.12\sin(\alpha)}{\alpha} + \frac{0.234\cos(\alpha)\sin(\alpha)}{\alpha} \right) \frac{0.47\sin^2(\alpha) + 0.49\cos(\alpha) + 0.51}{(-0.91\sin^2(\alpha) + 0.96\cos(\alpha) + 1)^2} \right. \\ & \left. + \frac{0.75\sin(\alpha)K(R_n)}{\alpha} + \frac{0.69\cos^2(\alpha)\sin(\alpha)}{\alpha K(R_n)} \right) \end{aligned} \right] \\ & - 3 \left(\frac{R_n}{R_0} \right)^2 \left\{ + \frac{2m\left(\frac{R_n}{R_i}\right)}{\sqrt{3}} + \frac{2mh(\alpha)}{\sqrt{3}} - 3\cos(\alpha)\ln\left(\frac{R_n}{R_i}\right) \frac{(-K(R_n)\cos(\alpha) + L(R_n))}{\alpha\sin^3(\alpha)} \right. \\ & - \frac{\sqrt{3}m\ln\left(\frac{R_n}{R_i}\right)(1 + \cos^2(\alpha))}{2\alpha} + 0.081\alpha + 8\ln\left(\frac{R_n}{R_i}\right) \frac{(-K(R_n)\cos(\alpha) + L(R_n))}{\sin^2(\alpha)} \\ & \left. + \frac{8mh(\alpha)}{\sqrt{3}} \ln\left(\frac{R_n}{R_i}\right) + \frac{8m\left(\frac{R_n}{R_i}\right)}{\sqrt{3}} \right\} \\ & + 5.2 \left(\frac{R_n}{R_0} \right)^2 (0.67J(R_n) - 0.67a(R_n) + 1.33) \\ & + \sqrt{3} \left(\left(\frac{R_n}{R_0} \right)^2 - 1 \right) \left(\frac{6\left(\frac{R_0}{R_n}\right)^4}{a(R_n)} + \left(\frac{R_0}{R_n} \right)^2 \left(1 - \frac{\frac{6\left(\frac{R_0}{R_n}\right)^2}{a(R_n)}}{1 + a(R_n)} \right) \right) \end{aligned} \right] \quad (A.3)$$

Substituting Eq. (A.3) to Eq. (7-36), the average forming pressure during the process can be defined as Eq. (A.4).

$$\begin{aligned}
& 0.01\alpha + \frac{\ln\left(\frac{R_n}{R_i}\right)}{\left(\frac{R_n}{R_0}\right)^2 \left(3 + \frac{9h(\alpha)\ln\left(\frac{R_n}{R_i}\right)}{2\alpha}\right) + 12\left(\frac{R_n}{R_i}\right)^2 \ln\left(\frac{R_n}{R_i}\right)} \times \\
& \left[\begin{aligned}
& \left(\sqrt{3} \left(\left(\frac{R_n}{R_0} \right)^2 - 1 \right) J(R_n) - a(R_n) + 2 \right) \\
& \left(\frac{0.05}{\alpha} - \frac{6K(R_n)\cos(\alpha) - 2}{\sin^2(\alpha)} \right. \\
& + \frac{6\ln\left(\frac{R_n}{R_i}\right)}{\sin^2(\alpha)} \left(\frac{0.12\sin(\alpha) + 0.23\cos(\alpha)\sin(\alpha)}{\alpha} \right) \left(\frac{0.47\sin^2(\alpha) + 0.49\cos(\alpha) + 0.51}{(-0.91\sin^2(\alpha) + 0.96\cos(\alpha) + 1)^2} \right) \\
& \left. + \frac{0.75\sin(\alpha)K(R_n)}{\alpha} + \frac{0.69\cos^2(\alpha)\sin(\alpha)}{\alpha K(R_n)} \right) \\
& - 0.5 \left(\frac{R_n}{R_0} \right)^2 + 10\sqrt{3}m \left(\frac{R_n}{R_i} \right) + 2\sqrt{3}mh(\alpha) \left(1 + 4\ln\left(\frac{R_n}{R_i}\right) \right) - \frac{3\sqrt{3}m\ln\left(\frac{R_n}{R_i}\right)(h^2(\alpha) + 1)}{2\alpha} \\
& + \frac{9\cos(\alpha)\ln\left(\frac{R_n}{R_i}\right)(\cos(\alpha)K(R_n) - 1)}{\alpha\sin^3(\alpha)} + 0.24\alpha \\
& - \frac{24\ln\left(\frac{R_n}{R_i}\right)(\cos(\alpha)K(R_n) - 1)}{\sin^2(\alpha)} - 2\sqrt{3}J(R_n) + 2a(R_n) - 4 \\
& + \frac{\sqrt{3}}{2} \left(\left(\frac{R_n}{R_0} \right)^2 - 1 \right) \left(\frac{6\left(\frac{R_0}{R_n}\right)^4}{a(R_n)} + \left(\frac{R_0}{R_n} \right)^2 \left(1 - \frac{\frac{6\left(\frac{R_0}{R_n}\right)^2}{a(R_n)}}{1 + a(R_n)} \right) \right) + \frac{mR_0}{T} \left(\frac{R_n}{R_0} \right) - \cos(\alpha)K(R_n) + 1 \\
& + \frac{mh(\alpha)}{\sqrt{3}} \ln\left(\frac{R_n}{R_i}\right) + \frac{m}{\sqrt{3}} \left(\frac{R_n}{R_i} \right)
\end{aligned} \right] \\
& - \frac{1}{\sqrt{3}} \left(\frac{R_n}{R_0} \right)^2 \left[\left(\left(\frac{R_n}{R_0} \right)^2 - 1 \right) J(R_n) - a(R_n) - 0.67 \frac{mR_0}{T} \left(\frac{R_n}{R_0} \right) c(R_n) + 2 \right]
\end{aligned}
\tag{A.4}$$

where

$$J(\alpha) = \ln \left(\left(\frac{R_n}{R_0} \right)^2 \left(\frac{1 + a(R_n)}{3} \right) \right)$$

$$K(\alpha) = \sqrt{1 - 0.91 \sin^2(\alpha)}$$

$$L(\alpha) = 0.087 \ln \left(\frac{1.96}{1 - 0.91 \sin^2(\alpha) + 0.96 \cos(\alpha)} \right) + 1 \quad (\text{A.5})$$

A-2- Necessary equations of the Upper Bound theory for extrusion process

Based on Fig. 7-2, the resulting strain rates for the extrusion phenomenon are

$$\dot{\epsilon}_{RR} = \frac{2v_R}{R} \quad \dot{\epsilon}_{\theta\theta} = -\frac{\partial v_R}{\partial R} \quad \dot{\epsilon}_{yy} = f(y, \alpha) \quad (\text{A.6})$$

$$\dot{\epsilon}_{R\theta} = \dot{\epsilon}_{\theta y} = \dot{\epsilon}_{yR} = 0 \quad (\text{A.7})$$

On the other hand

$$dV = 2\pi R^2 \sin\theta dR d\theta \quad (\text{A.8})$$

Therefore

$$\dot{W}_i = \frac{2}{\sqrt{3}} \sigma_0 \int \sqrt{\frac{1}{2} \dot{\epsilon}_{ij} \dot{\epsilon}_{ij}} dV = \frac{4\pi}{\sqrt{3}} \sigma_0 \int_{R_i}^{R_n} \int_0^\alpha \frac{3v_y}{R^2} + \frac{v_y}{4R^2} (\cos\theta) (R^2 \sin\theta) d\theta dr \quad (\text{A.9})$$

Combination of the Eq. A.9 and Eq. 7-19 leads to

$$\dot{W}_i = 2\pi\sigma_0 V_p R_n^2 \ln\left(\frac{R_n}{R_i}\right) f(\alpha) \quad (\text{A-10})$$

The same procedure could follow to obtain the value of the friction energy loss.

$$\dot{W}_f = \int k \Delta v dS = 2\pi \frac{\sigma_0}{\sqrt{3}} V_p R_n^2 \left(\frac{\alpha - \cos\alpha \sin\alpha}{2\sin^2\alpha} + R_i^2 (\cot\alpha) \ln\left(\frac{R_n}{R_i}\right) + m \frac{R_n^2}{R_i^2} \right) \quad (\text{A.11})$$

The total power required for extrusion process can be obtained by summing the internal power and the power dissipated on all frictional and velocity discontinuity surfaces. The power equation is a function of semi-cone angle. The optimum angle that minimizes the total power can be determined by differentiating the total power with respect to the angle.

$$\frac{\partial \dot{W}}{\partial \alpha} = 0 \quad (\text{A.12})$$

which yields to

$$\alpha = \sqrt{\frac{3}{2} m \ln(R_n / R_i)} \quad (\text{A.13})$$